NOVEMBER 1959

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4 GROSVENOR GARDENS LONDON SW1

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VOLUME 193 PART 3

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Palette on Production

No. 3 in a series of original paintings specially commissioned from Robert Johnston R.I., S.M.A., illustrating the productive resources for steelworks plant of Davy-United's Darnall Works. This scene in one of the Heavy Machining Shops shows the machining of an eighty ton roll housing for a blooming and slabbing mill.

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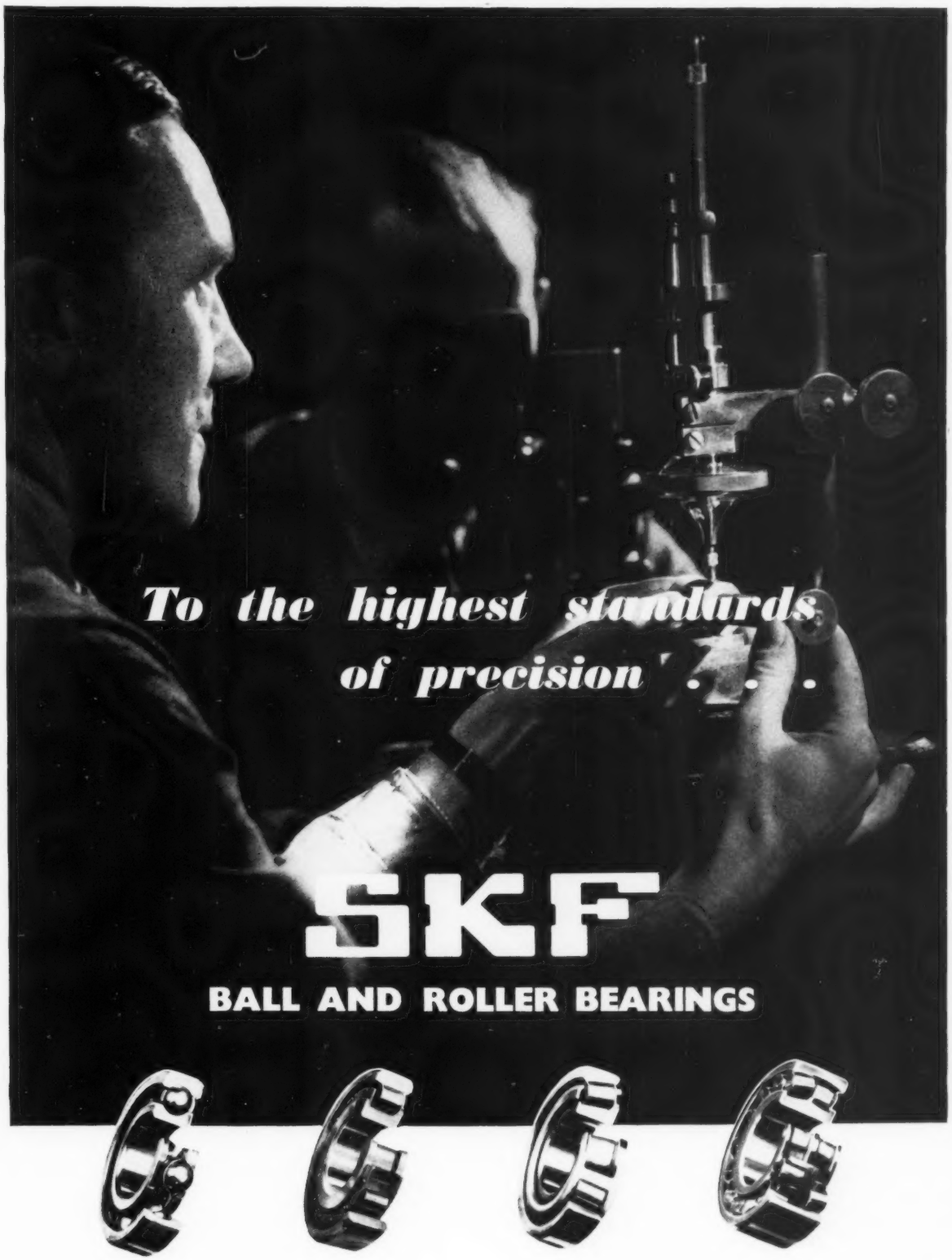

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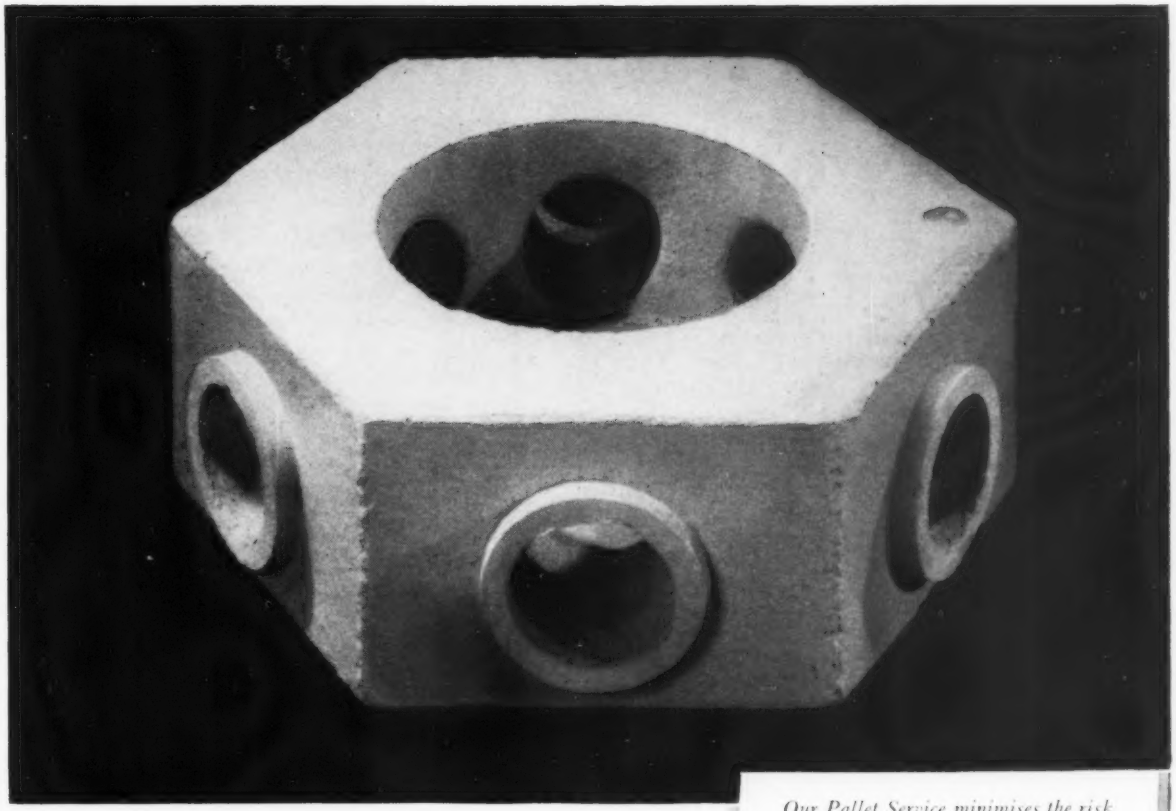

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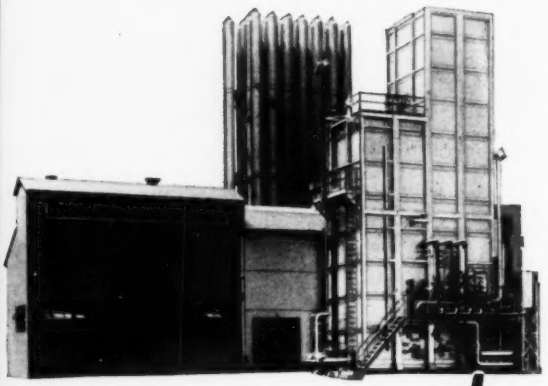

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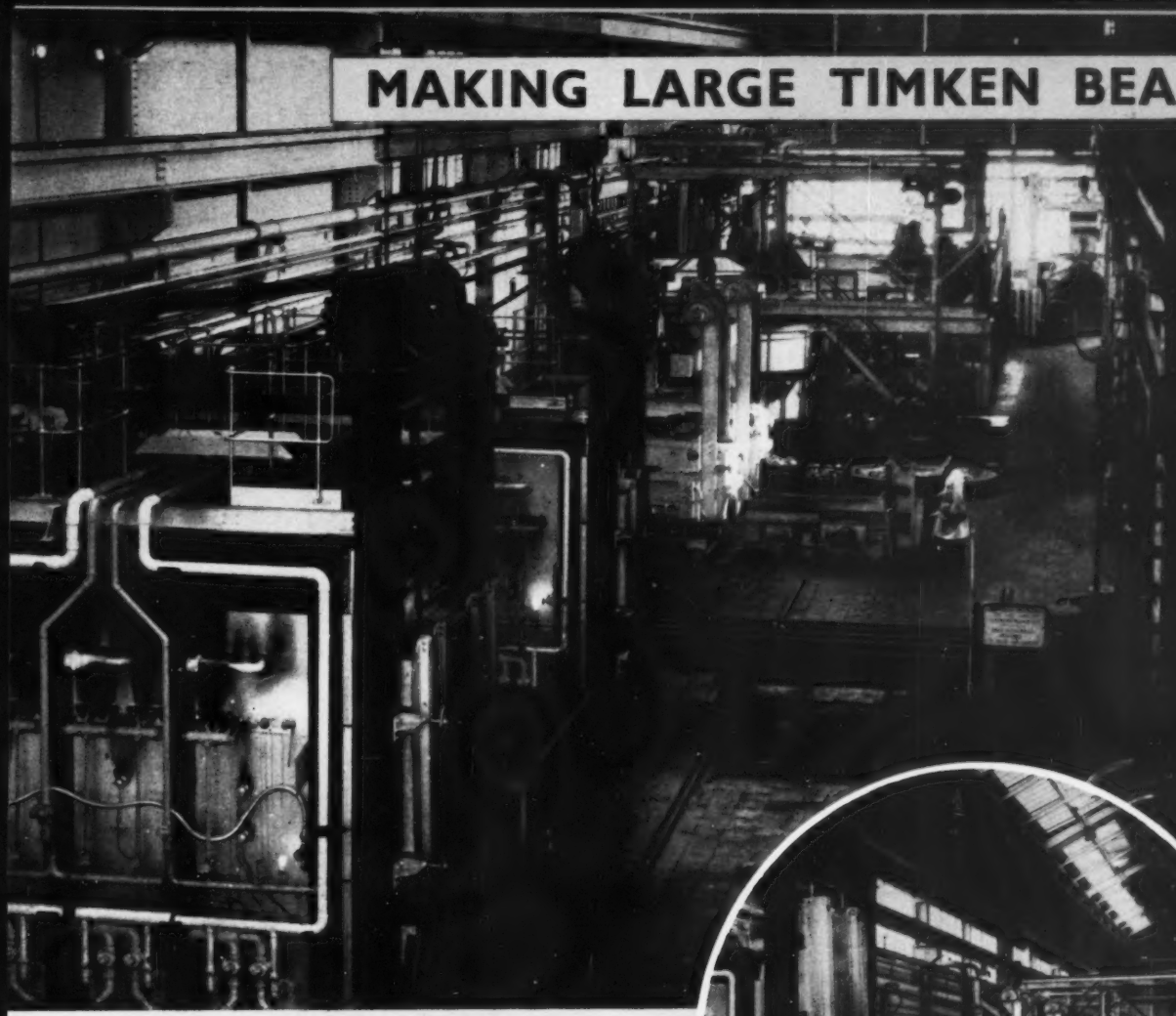

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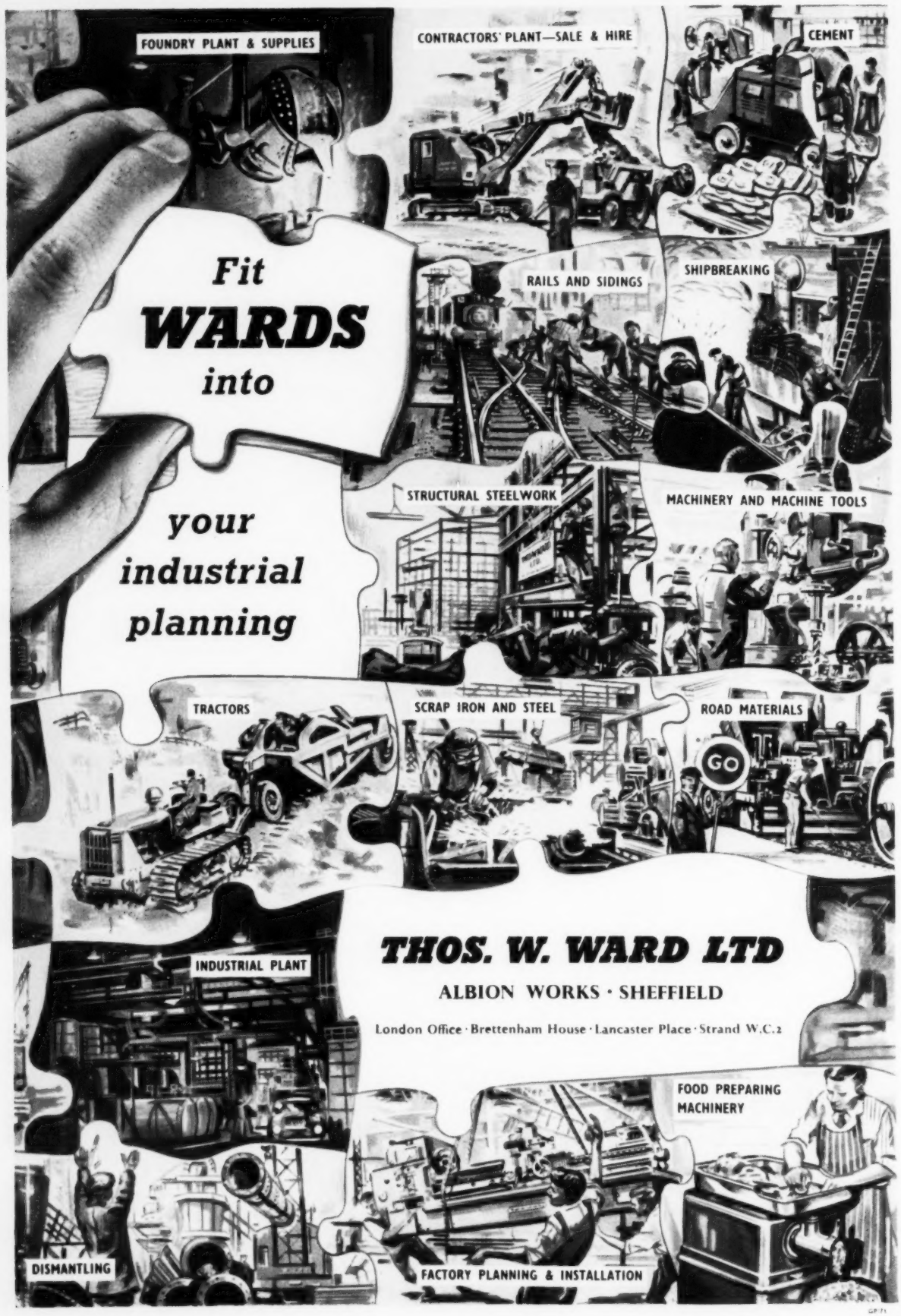
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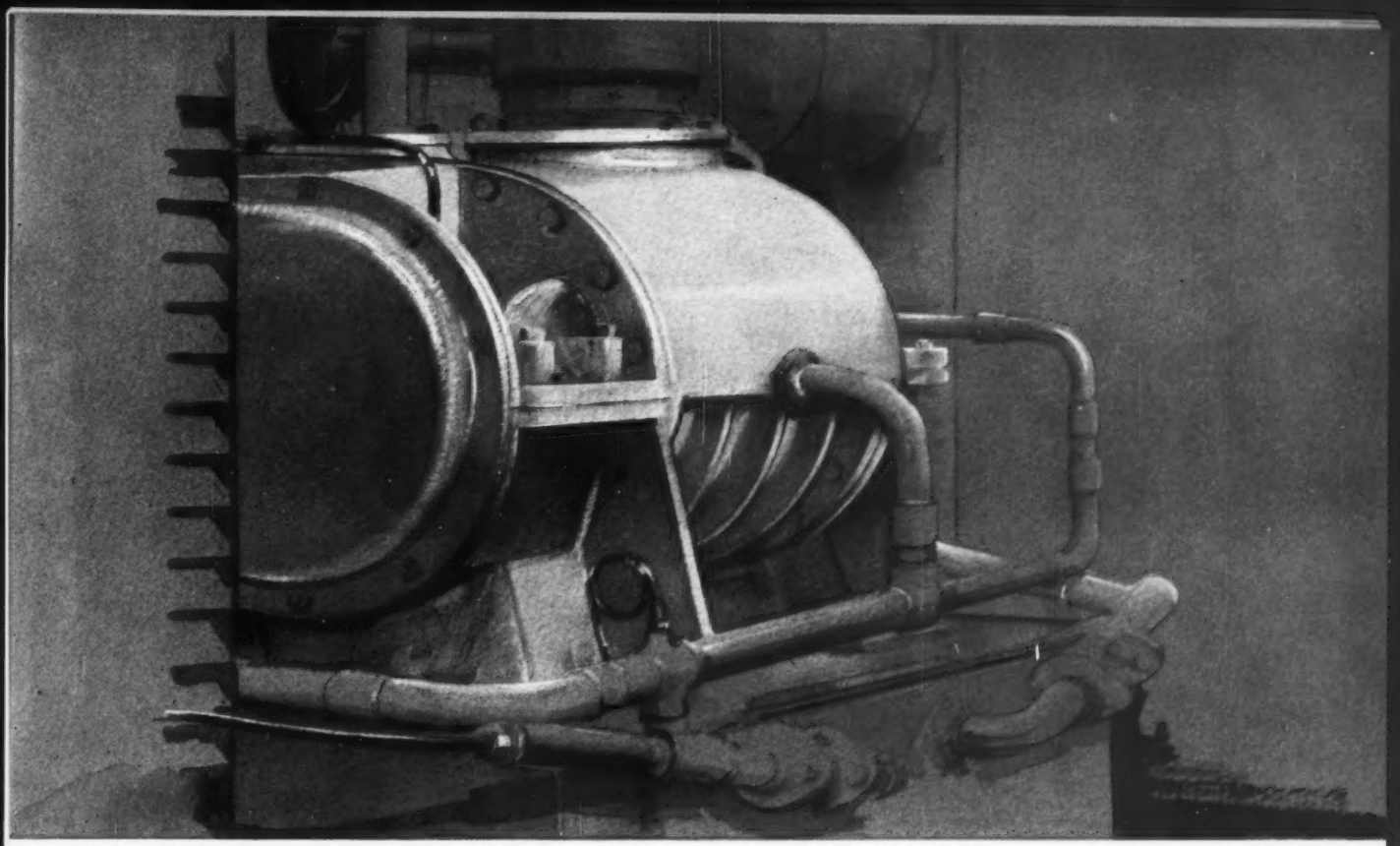

Howden compressor at the National Gas Turbine Establishment, Farnborough: (Crown Copyright reserved)
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Howden compressors are driven through Howden speed-increasing gearboxes from any available power source.

The range of Howden compressors :—
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Howden single-stage compressors may also be used as exhausters for vacuum down to 1/5 of the prevailing atmospheric pressure: two-stage for vacuums to 1/15 of atmospheric pressure.

Howden compressors have been supplied for: Pneumatic conveyors; Work's compressed-air service; Distribution of Town's gas; As boosters for high-pressure air or gas; As exhausters for evaporating plants. In the chemical industry; compressing butane, carbon dioxide, nitrogen, propane and steam.

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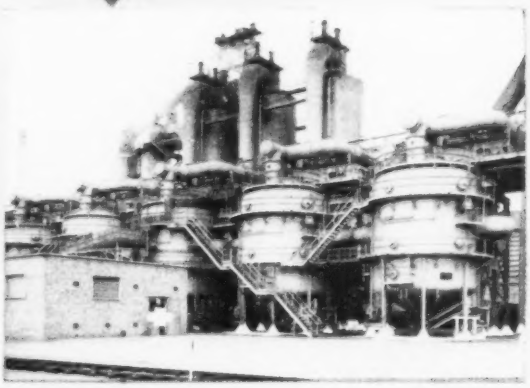
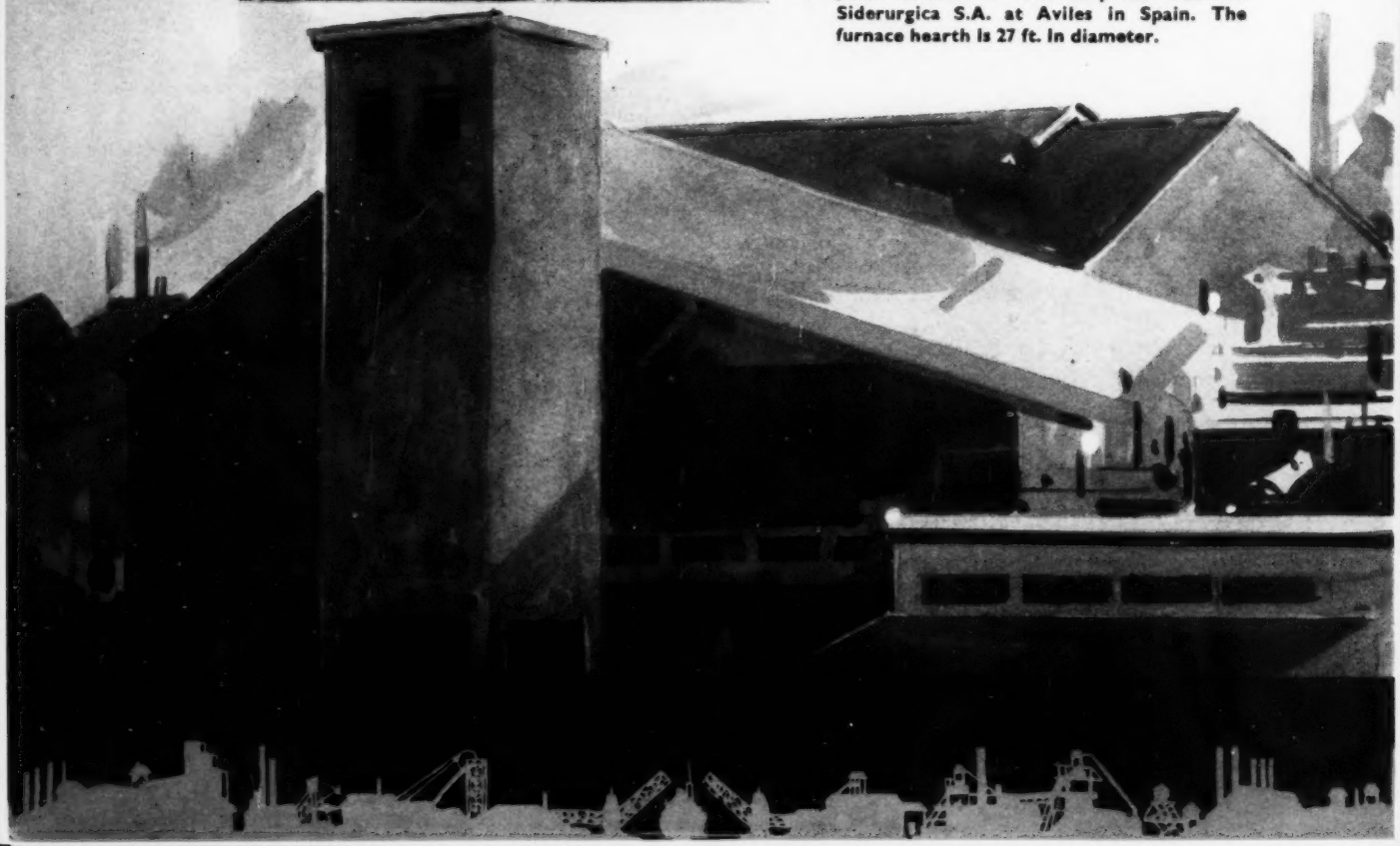


Blast furnace for the Empresa Nacional Siderurgica S.A. at Aviles in Spain. The furnace hearth is 27 ft. in diameter.



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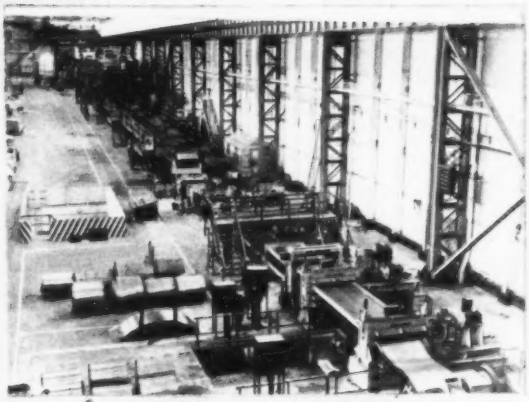

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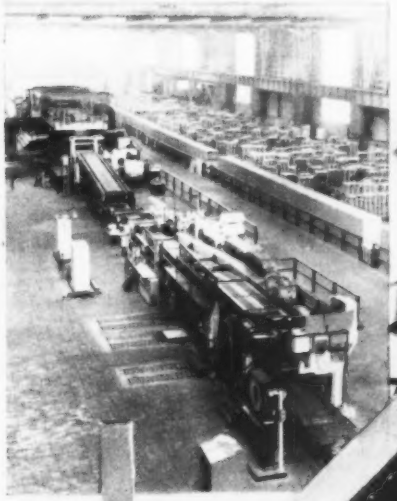

Electrolytic tinning line at the Velindre Works of the Steel Co. of Wales.

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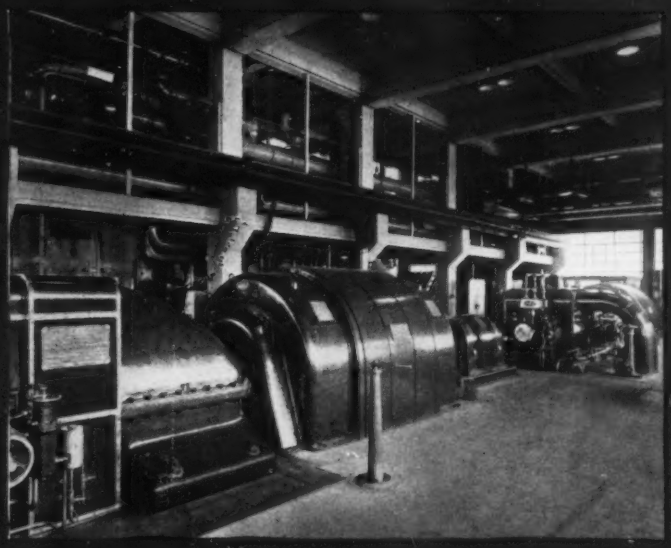

Above - The turbo-generator
Above right - The water tube boiler

A general view of the power station

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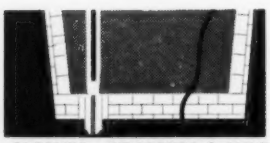

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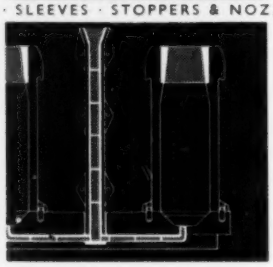

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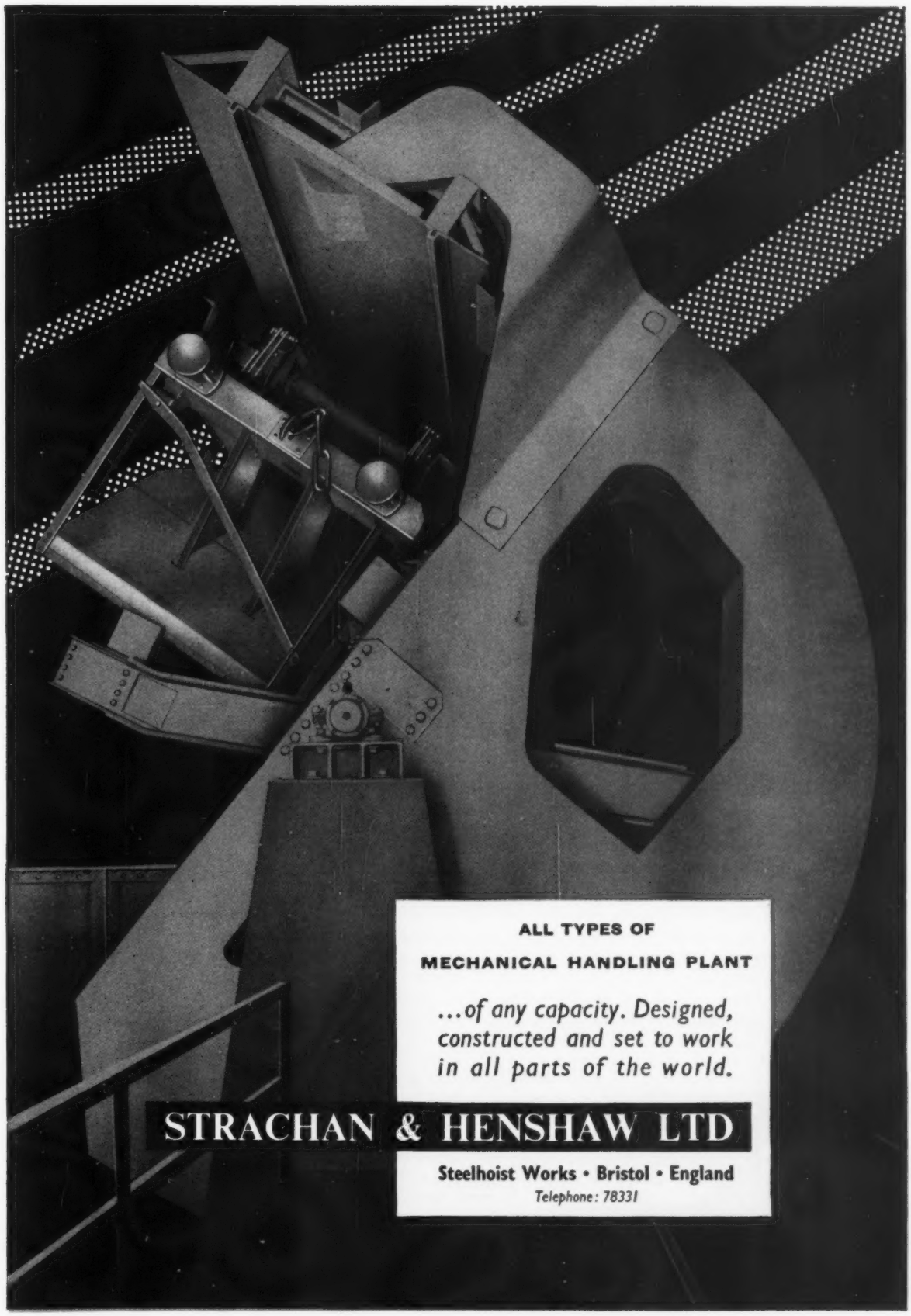
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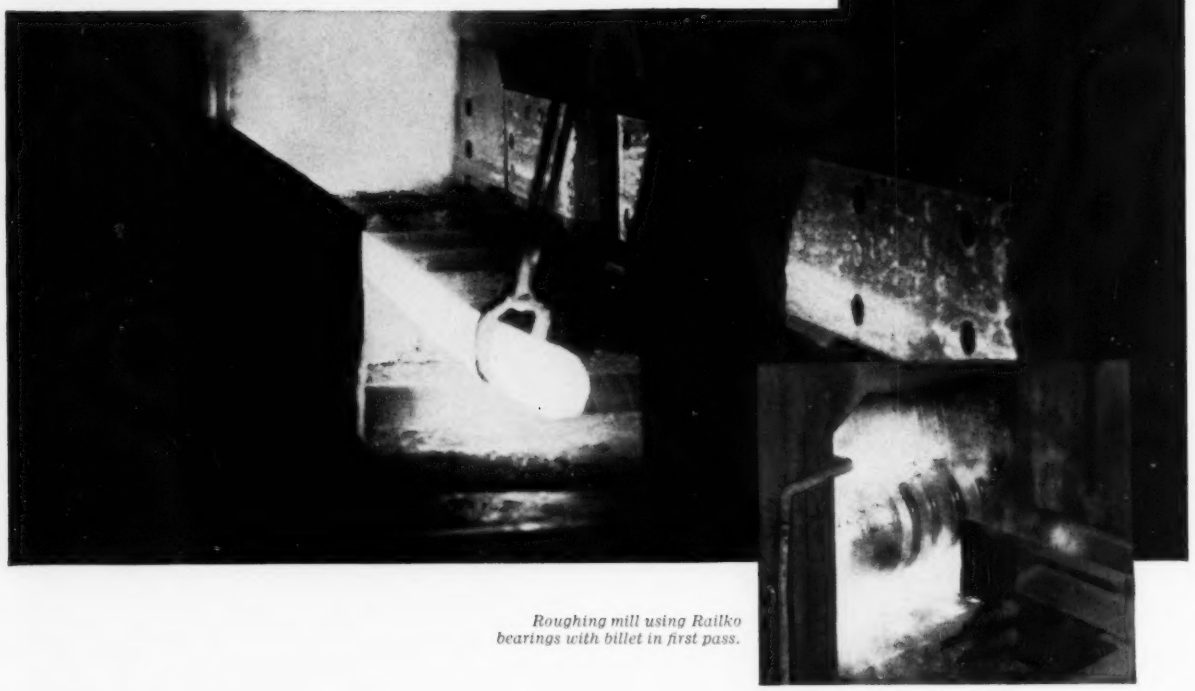

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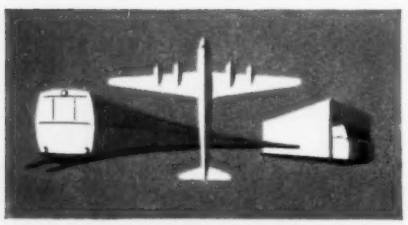

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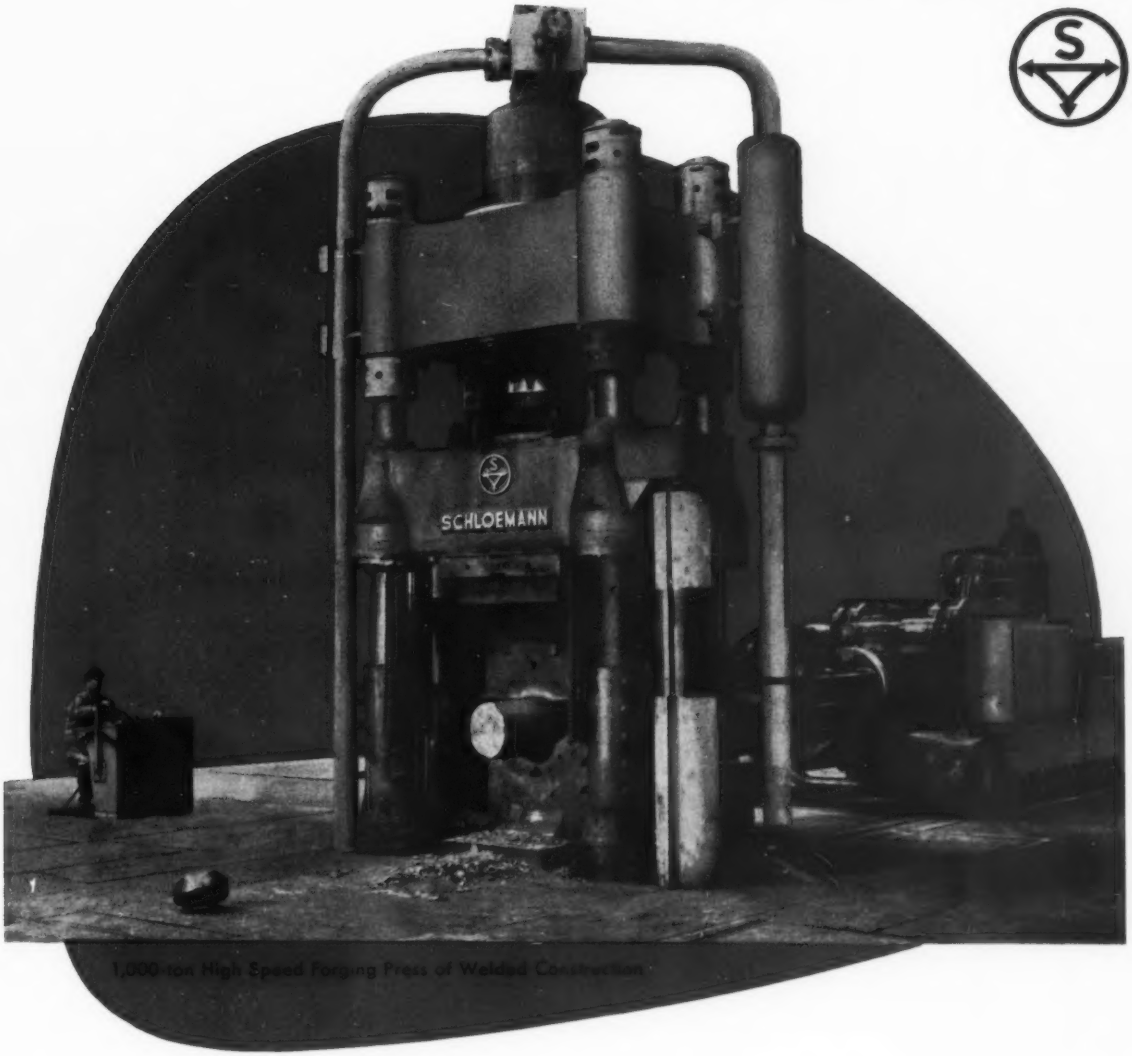

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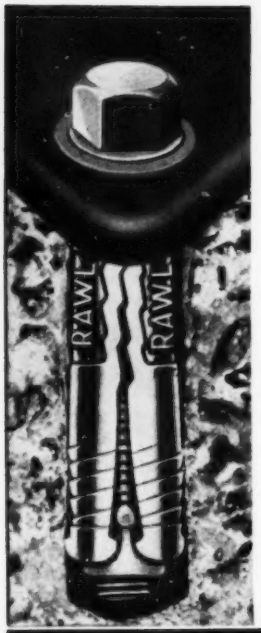

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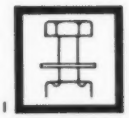
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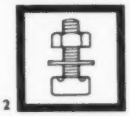
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Fig. 3. Pipehanger Rawlbolts (with adaptors for use with Pipehangers and other gas fittings).

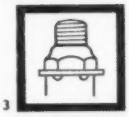
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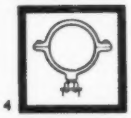
Fig. 4. Rawlbolts fitted Pipeclips (7 sizes for $\frac{1}{8}$" up to $2\frac{1}{2}$" O/D pipe diameter).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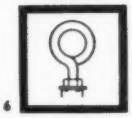
Figs. 5 & 6. Hook Rawlbolts and Eye Rawlbolts for providing anchorage for guy ropes and cables. supporting suspended ceilings etc. (5 bolt diameters, 3/16" up to $\frac{1}{2}$").

5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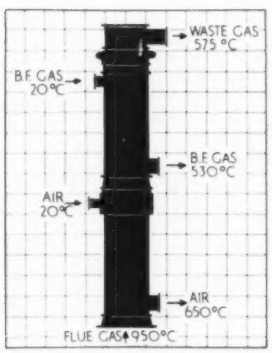

FOR EXAMPLE

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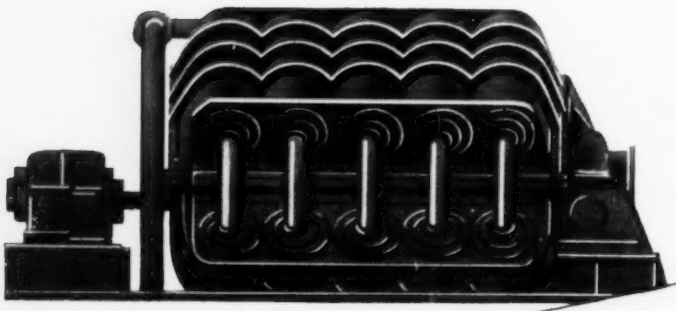

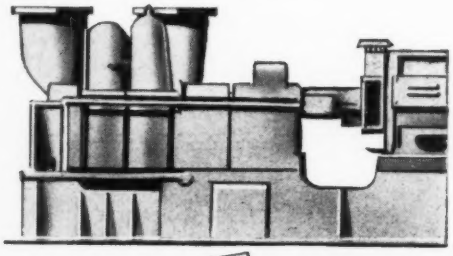

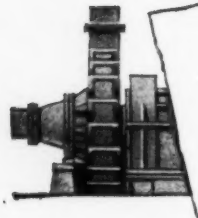

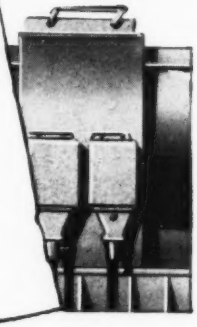

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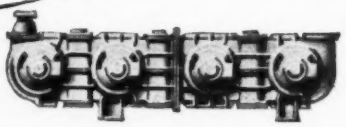

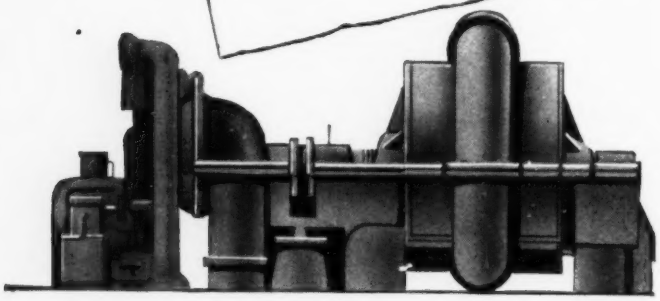

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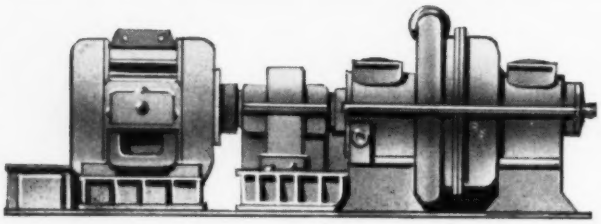

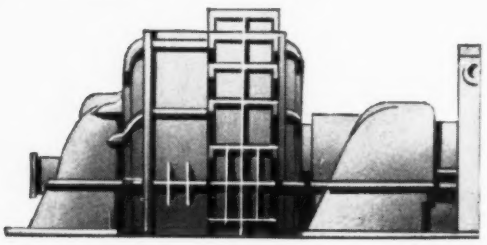

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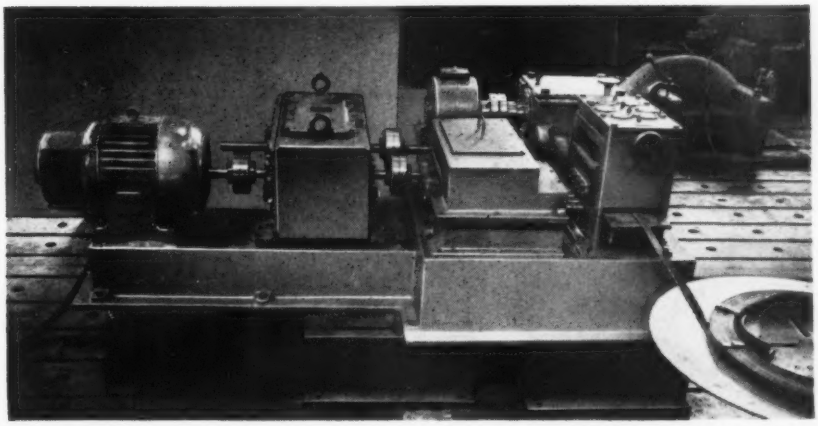
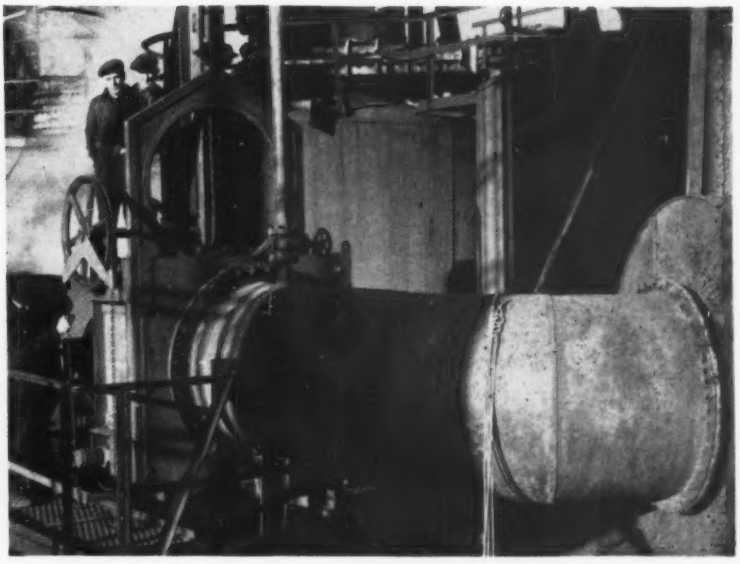


(*left*) Automatic Cutting and Straightening Machine for Barrow Steelworks Ltd. (*reproduced by permission*).

BELOW

(*left*) 4" Barrow-Kinney Heavy Duty Self Cleaning Spray in operation.

(*right*) 42" Barrow-Kinney Plate Type Gas Valve erected in position.



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Photograph taken shortly before repainting.

Ensbury Bridge, Dorset. Photograph by courtesy Dorset County Council

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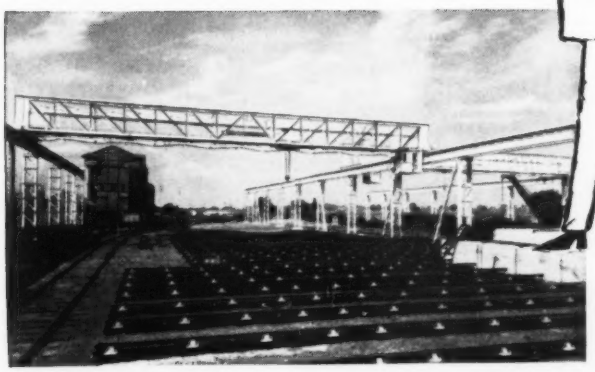
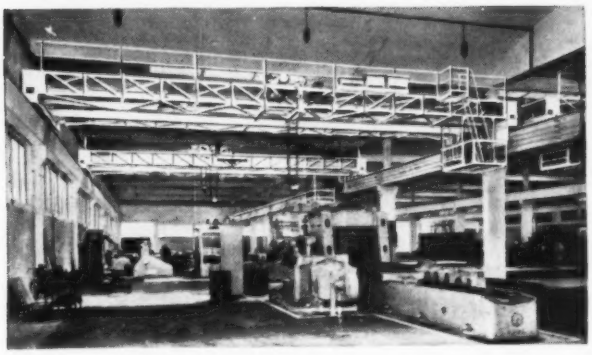
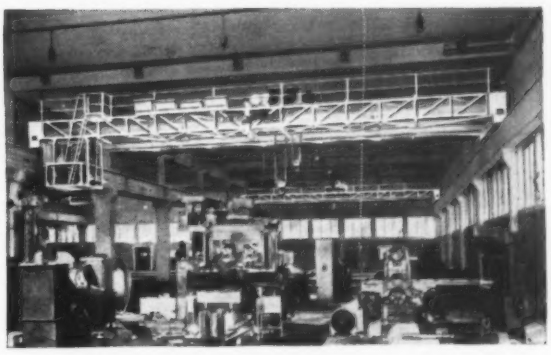


Photographed by permission of the British Transport Commission



One of the 2½ ton cranes, with a 5 ton crane in the background



One of the three 5 ton cranes which handle the completed units

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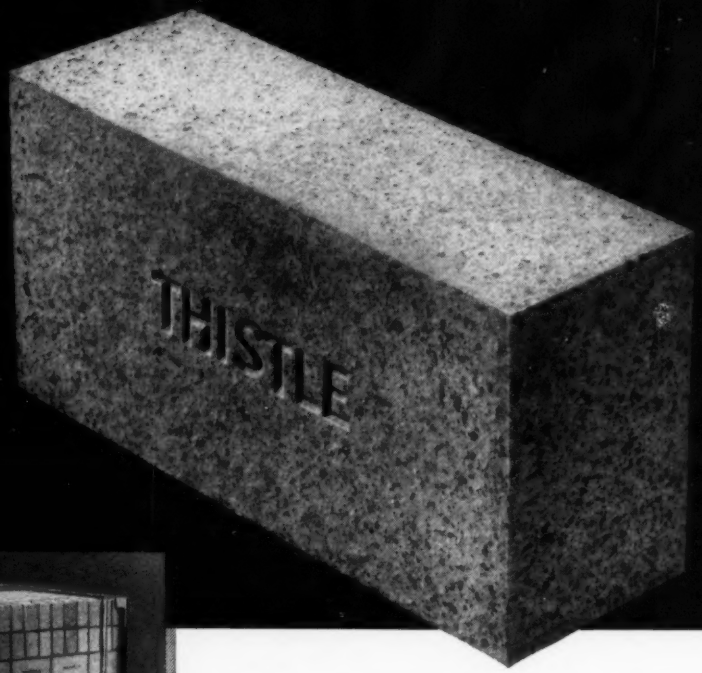

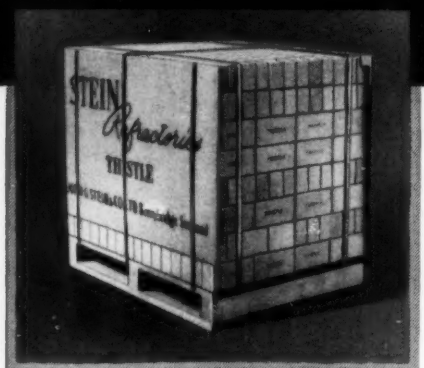

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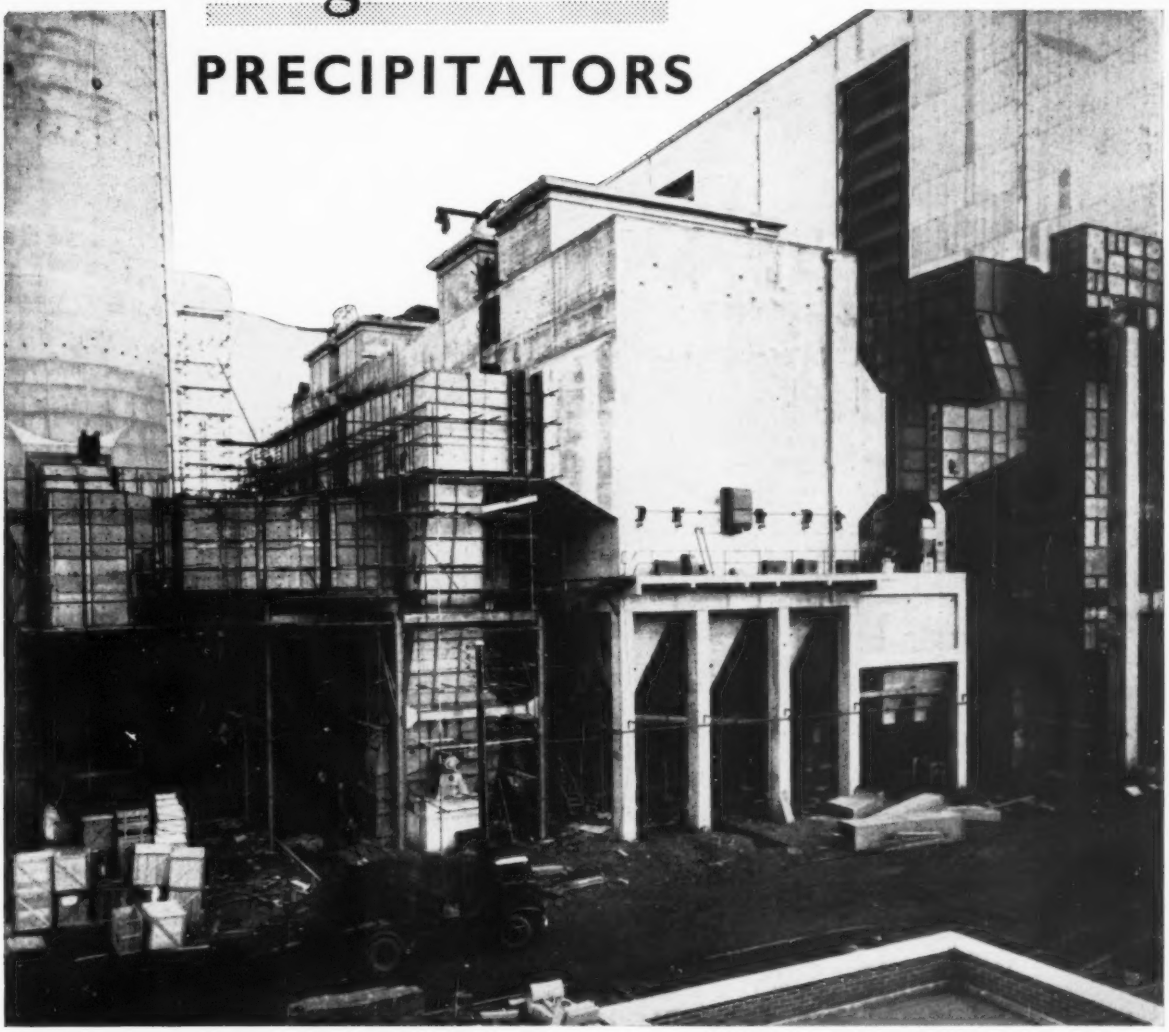

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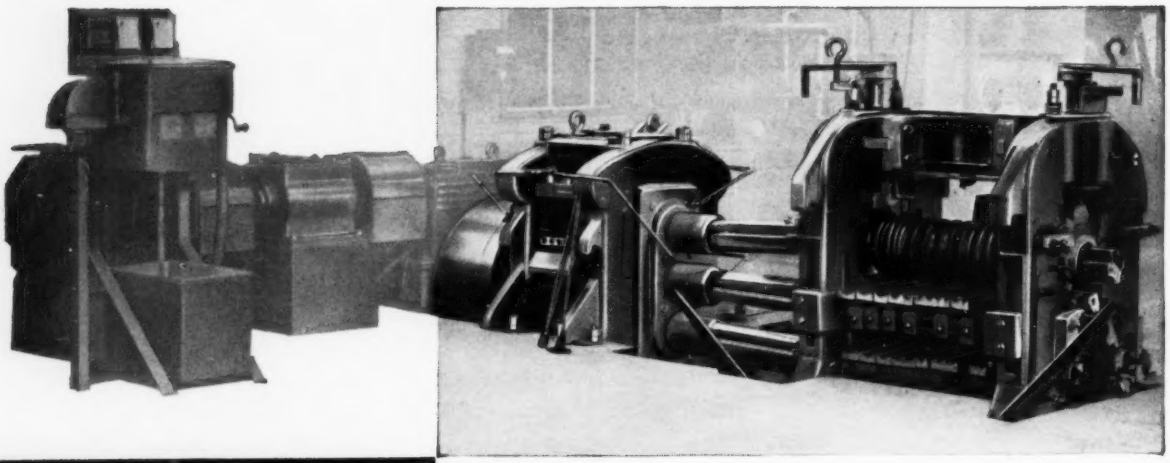

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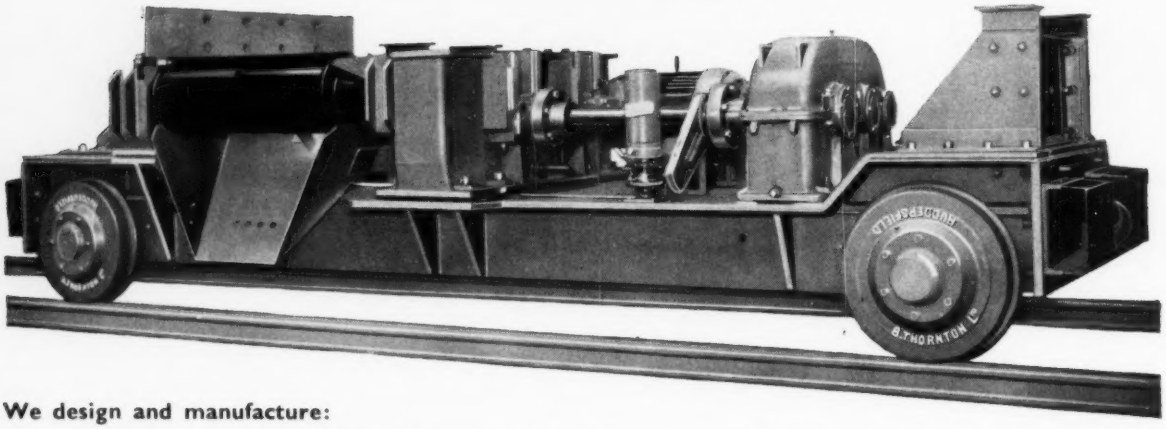

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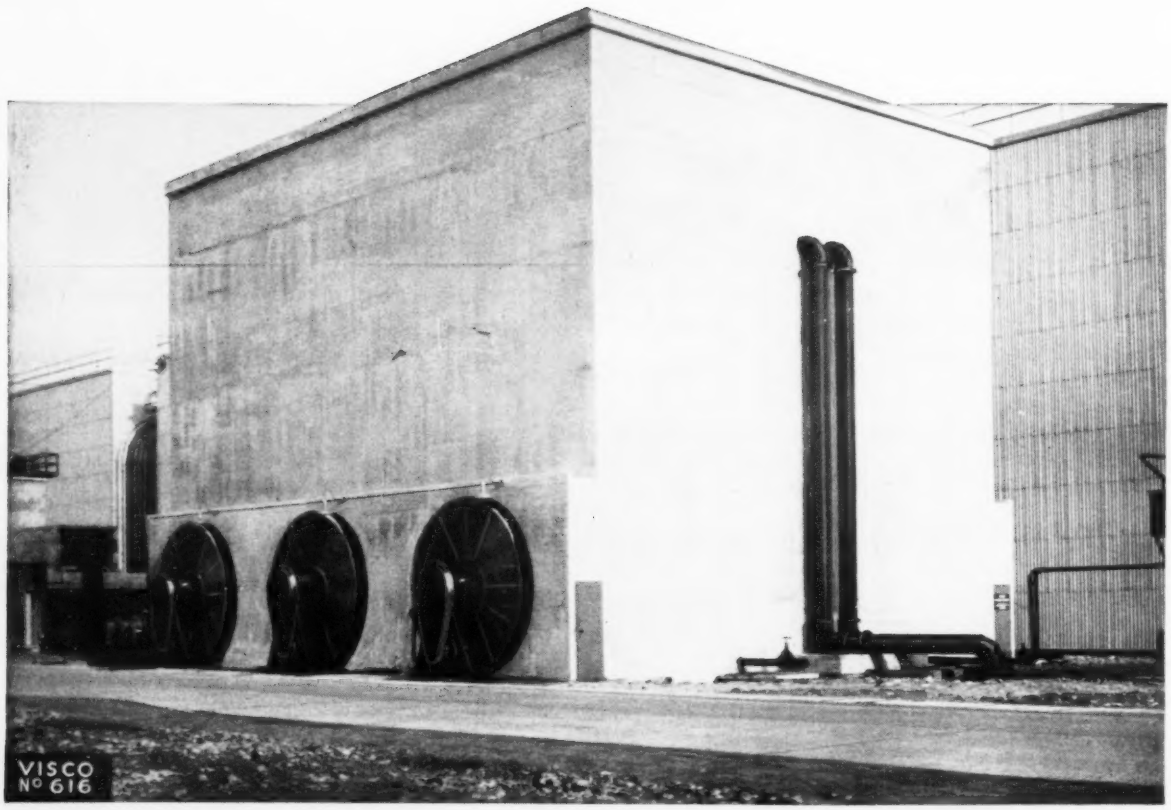

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BLAST FURNACE GAS CLEANING

Left—A two-section Holmes-Elex Electrical Precipitator installed for the final cleaning of blast furnace gas at the Scunthorpe Works of the Appleby-Frodingham Steel Company (Branch of the United Steel Companies Limited).

BLAST FURNACE GAS CLEANING

Right—Three irrigated Holmes-Elex Electrical Precipitators preceded by Wash Towers cleaning blast furnace gas at the Bilston Works of Messrs. Stewarts & Lloyds Limited.

FERRO-MANGANESE GAS CLEANING

Bottom—Three Holmes-Elex Electrical Precipitators cleaning ferro-manganese gas after the blast furnaces at the works of the Workington Iron & Steel Company (Branch of the United Steel Companies Limited)

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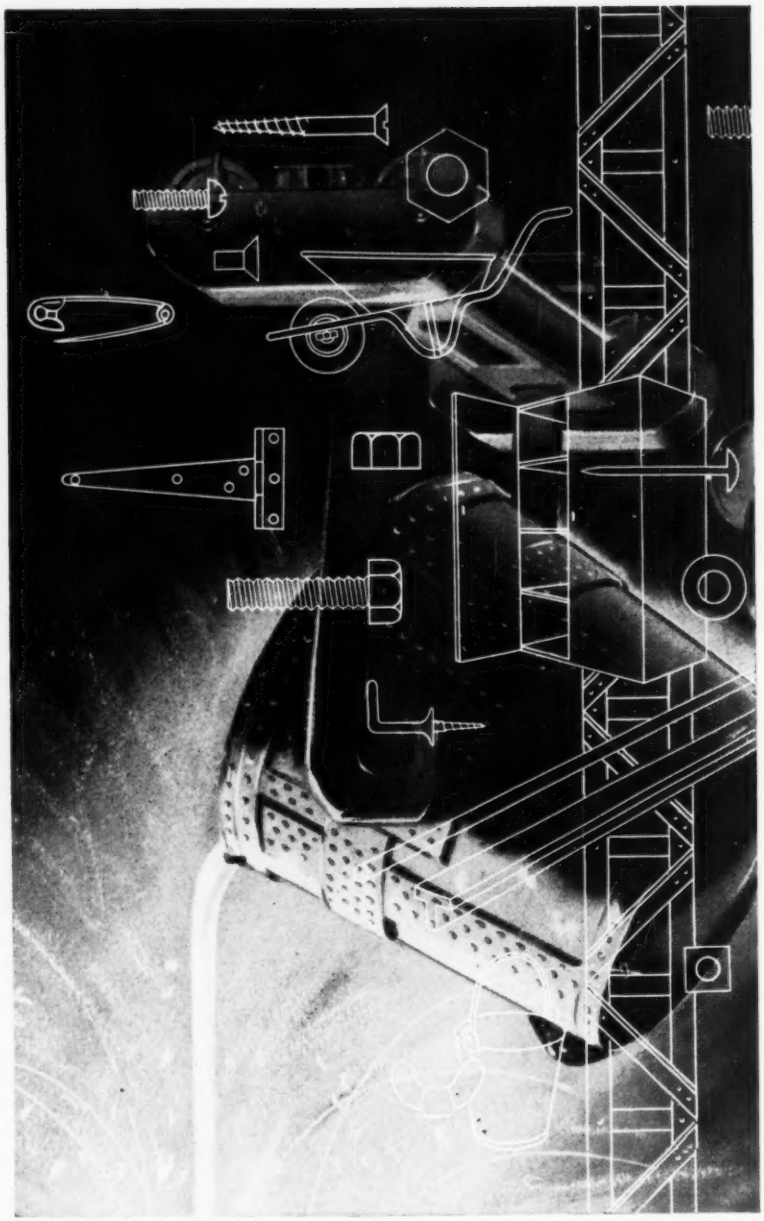

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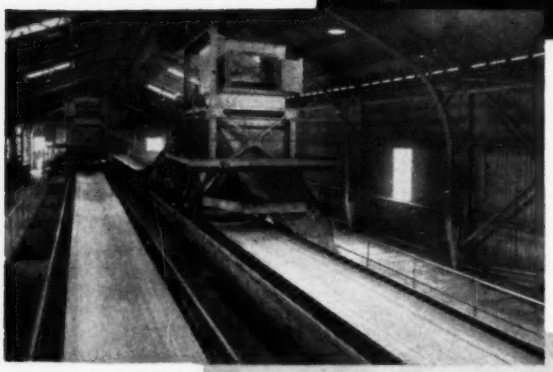

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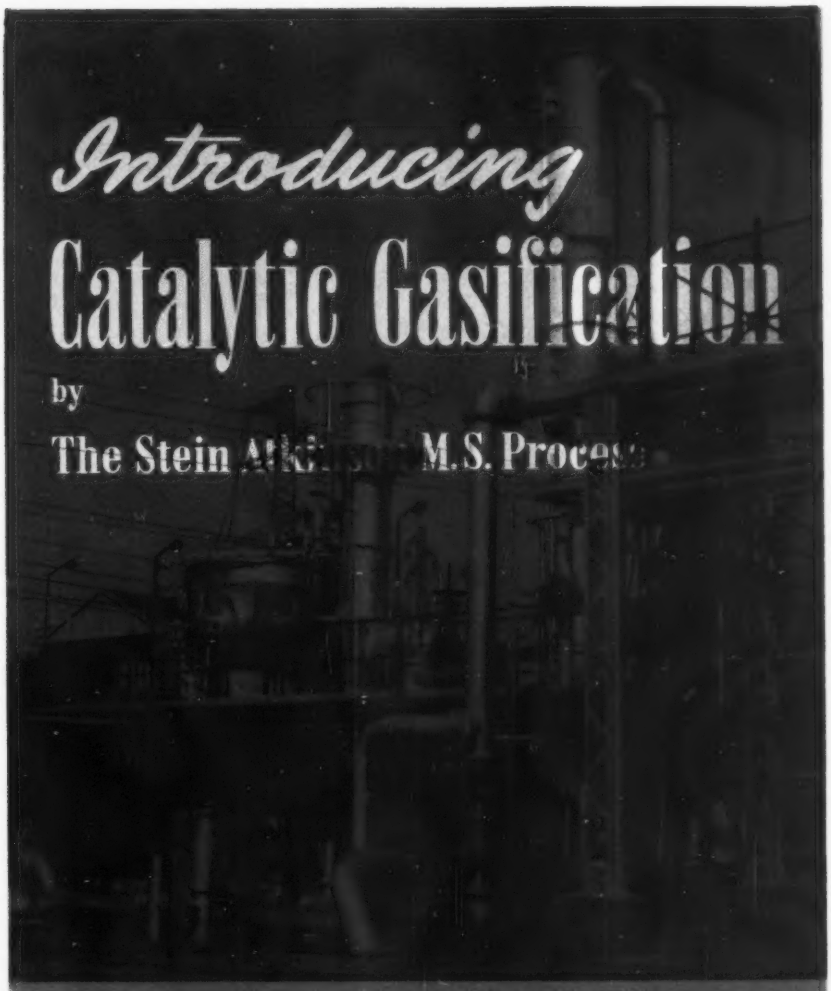

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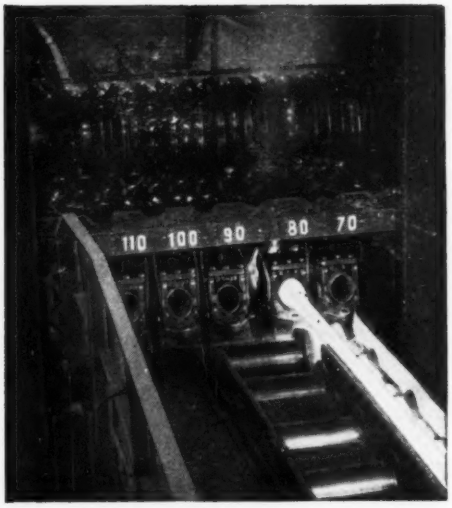

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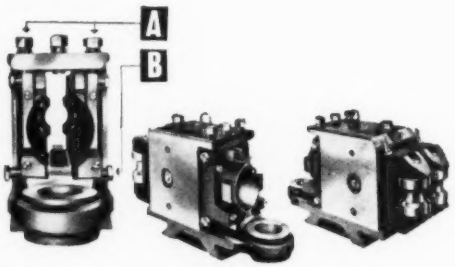
2 The rollers are mounted on leaf springs, which can be adjusted by means of the screws "B", and hold the oval steadily even if there should be some slight variation in the thickness of the oval.

3 If the thickness of the oval should vary, the springs of both rollers yield an equal amount, so that the oval remains in the centre of the groove.

4 To tilt the oval, the screws "A" are adjusted. One roller will then be lifted as much as the other is lowered.

5 As the oval is held very rigidly, the leader oval for wire rod may be thick, unless other circumstances call for a thinner oval.

6 The roller guide assemblies are narrow, and all screws that require adjustment during rolling are accessible from the front of the mill. No lateral space outside the guide box is therefore required for gaining access to the set screws, and the guide box assembly can thus be located close to the mill housing.



7 As the groove in the entry guides is wide and nothing but rolling friction acts on the oval, the latter is very easily introduced into the pass, and there is practically no risk of scratching the bar.

8 The roller guide may be used not only for leader and drawing oval passes but also for edging flats as well as for rolling squares, hexagons, octagons and various other sections, for instance in reduction passes for hexagon. It is suitable for all types of rolling mill. An important advantage is that the roller guide enables repeaters to be used in rolling plump leader-ovals.

PS *Practically speaking, every bar and wire rod mill in Scandinavia uses roller guides of our design.*

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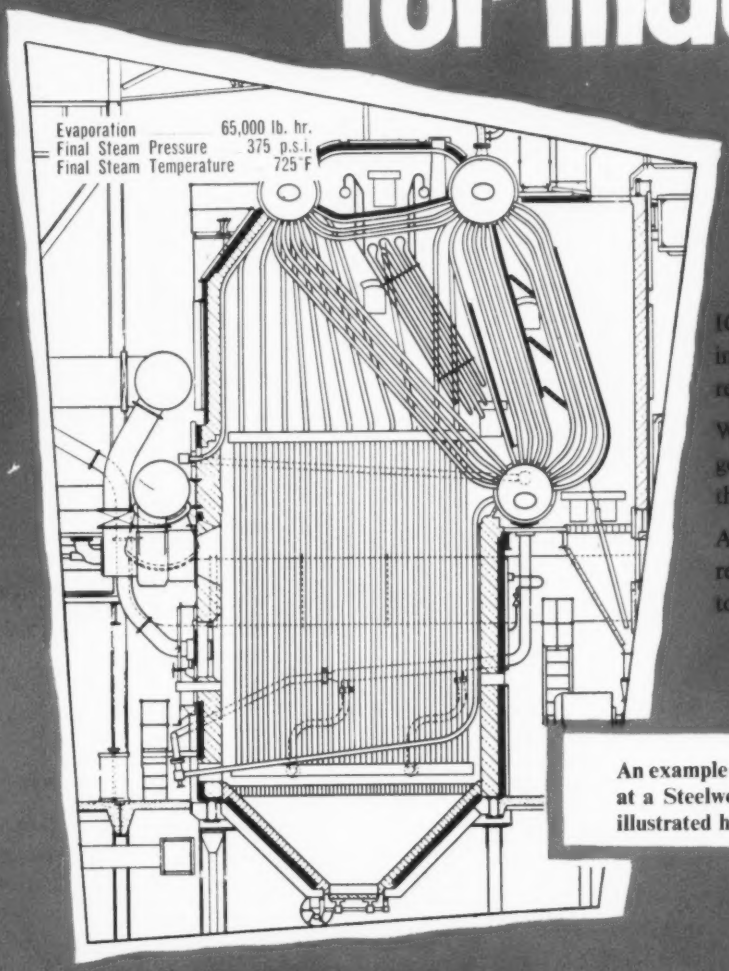
Steam raising for industry



IC build boilers to suit the individual steam raising requirements of every industry.

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An example of an installation at a Steelworks in Turkey is illustrated here.

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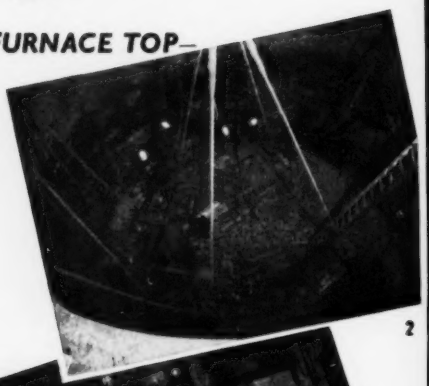
FURNACE TOP—



2

3

4

—TO FURNACE BOTTOM

1 *'Lumpy going'—half-way down hearth.*

2 *Men removing throat armour.*

3 *Cat Traxcavator commencing, digging, Cast House Floor Level.*

4 *The two 'Cats' meet, final 'break through'.*

Scenes of operations at No. 1 Blast Furnace 1
(photos by permission of John Summers & Sons Ltd., Shotton)

TOTAL TIME wrecking burden, lining, hearth and bear 18 **DAYS.**
(contract carried out in association with Salem Engineering Co., Ltd.)

And here are the details of our latest contract, at Richard, Thomas & Baldwins Ltd., 'C' Furnace, Ebbw Vale.

Demolition commenced 7th August, 1959
Demolition completed 21st August, 1959

TOTAL TIME wrecking burden, lining hearth and bear 14½ **DAYS.**

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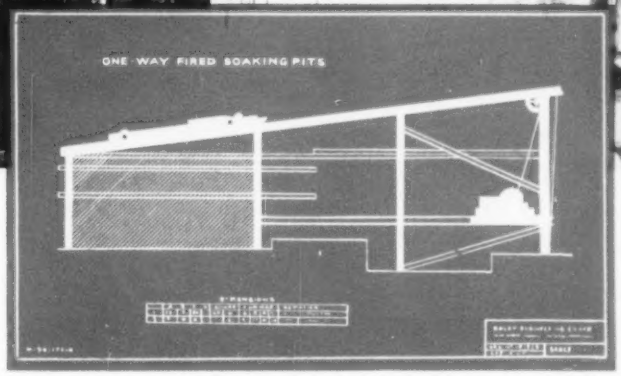

The cover withdrawal gear is normally designed as shown in diagram, so that the cover runs up a slight incline. It can be supplied, if required, so that the cover withdraws on horizontal rails.

one-way fired soaking pits

When space is at a premium, Salem One-way Fired Soaking Pits with their patented features are the ideal solution for Ingot Heating. An outstanding feature is the placing of the cover withdrawal gear below platform level away from the heat— not on top of the cover. These one-way fired pits are designed to take up to 100 tons per charge and for operation on any type of fuel.

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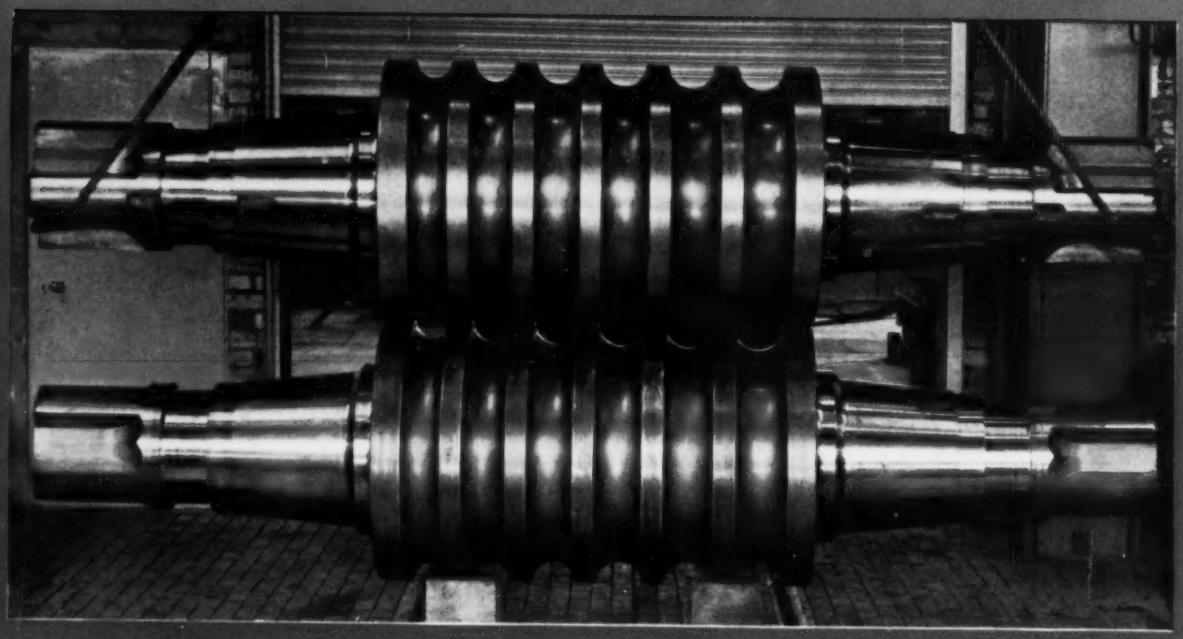

A pair of our Special "SSXH" Mooralloy Quality Rolls for Finishing Large Rounds 4¼" to 6" dia. for No. 6 Stand Roll size 32" dia. x 54" long barrel

The Specialised Products include:

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SPECIAL ROLLS FOR STAINLESS STEEL ROLLING

PART CHILL, GRAIN & ALLOY GRAIN ROLLS

ALSO THE S.G. RANGE OF SUPER TOUGH SPHEROIDAL GRAPHITE ALLOY IRON ROLLS

No. 5 Vertical Edging Stand in our Mooralloy "SSXM" Quality for Billet train (diamond pass) Leader for 2" to 4" Billets. Roll size 31½" dia. x 18" long barrel.

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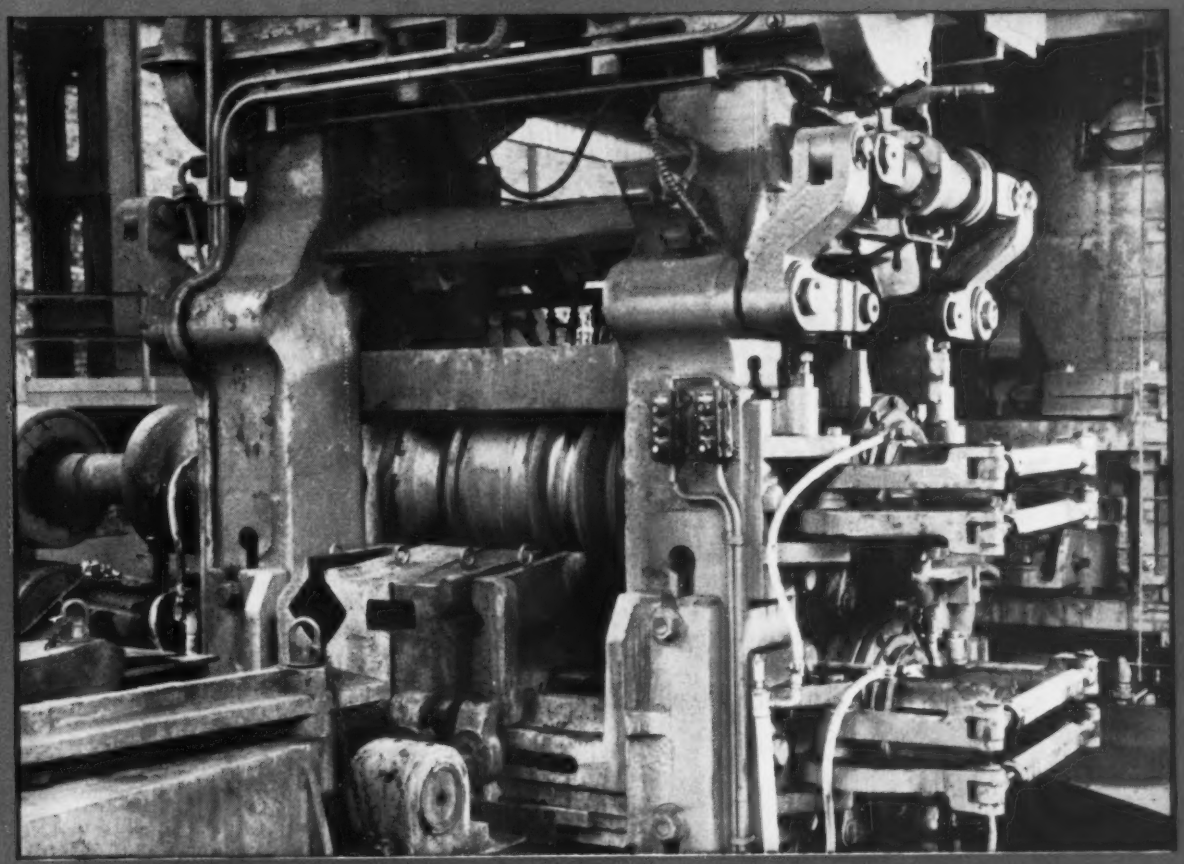

No. 6 Stand Finishing for 4", 4½" and 5" Billets, also Roughing for 2" to 3½" Billets and Slabs at Consett Iron Co., Ltd showing our Special S.S.X. Mooralloy Quality Rolls. Roll size 32" dia. x 54" long barrel.

Photographed by the courtesy of The Consett Iron Co. Ltd.

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"SSX", "SSXM", "SSXH".

The "XL" Range of Tough Medium Hard Metal
"XLM", "XL", "XLH".

The "XXL" and "XXXL" Ranges of Hard Superfine Close Texture Metal
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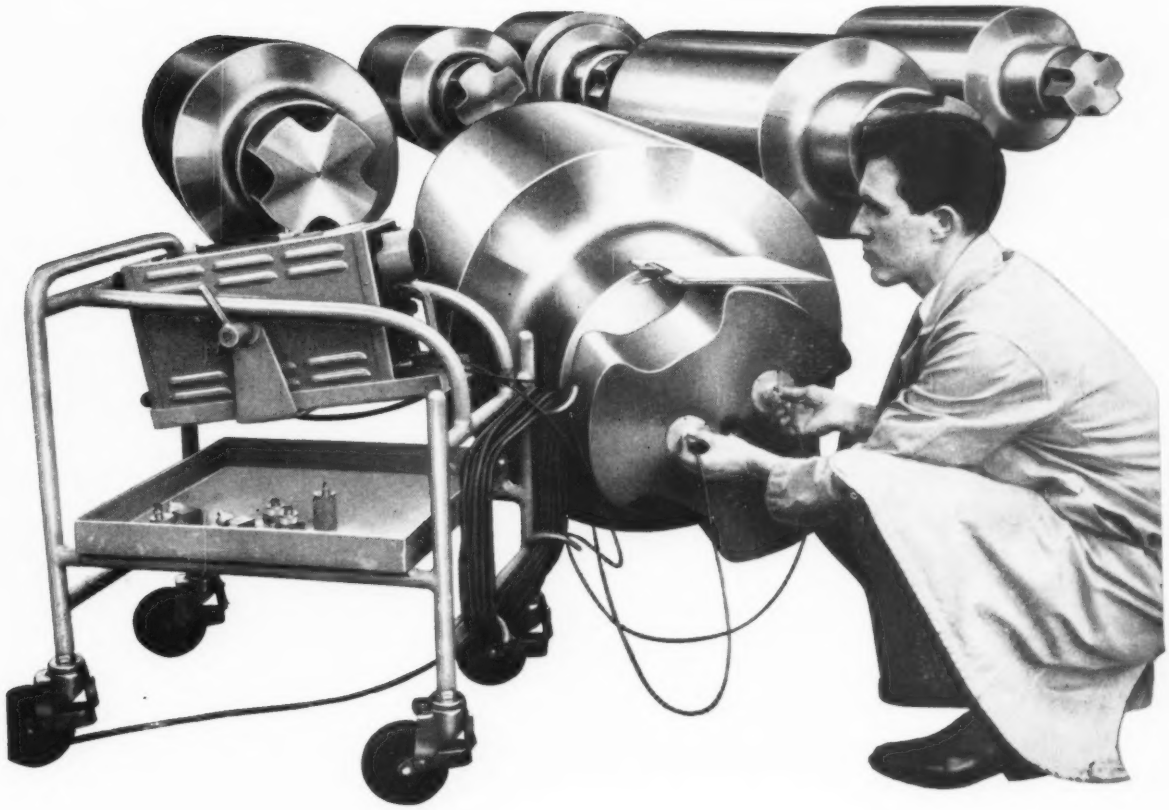

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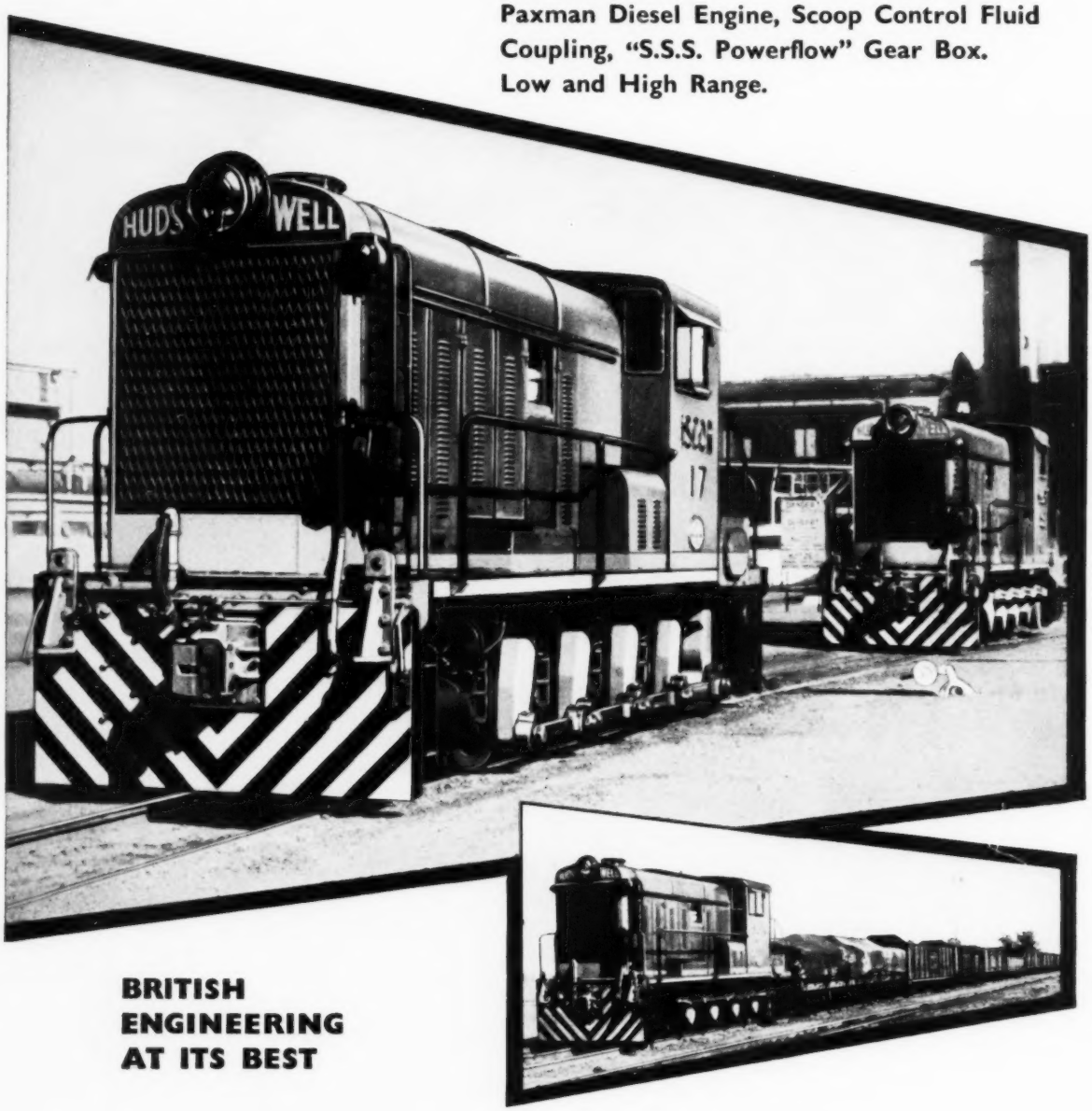

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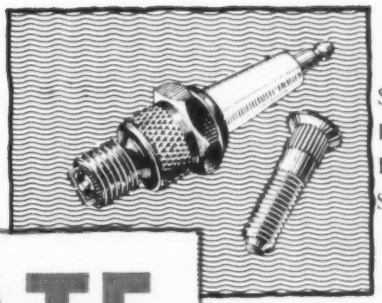

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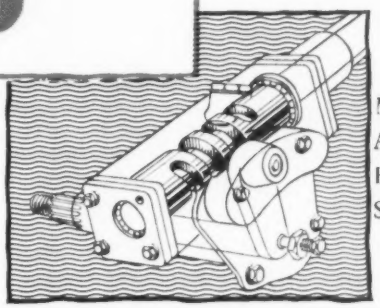

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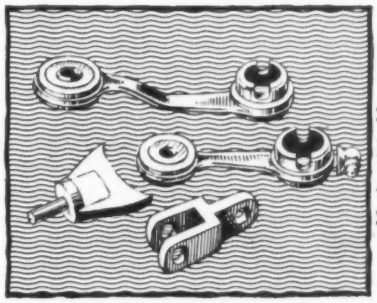

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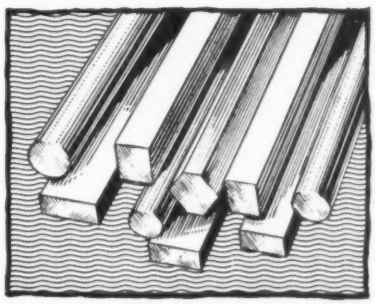

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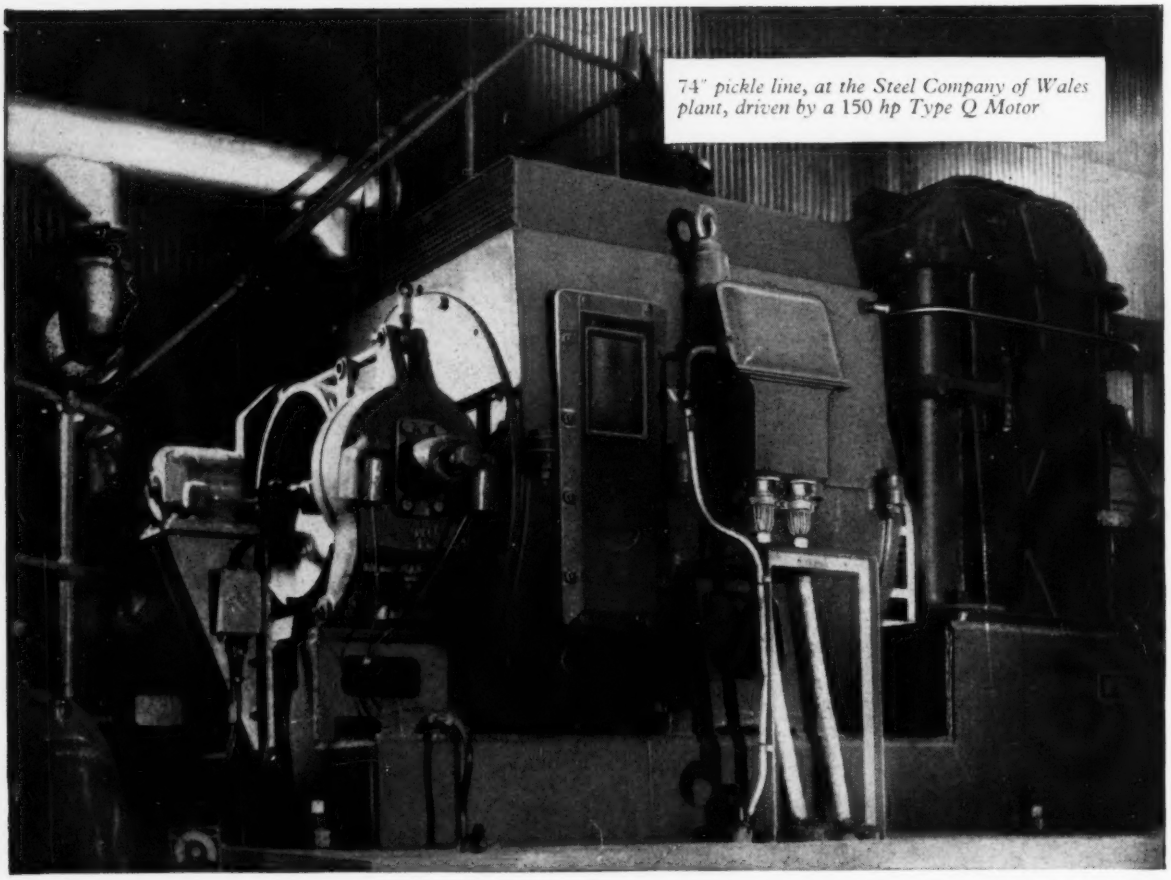

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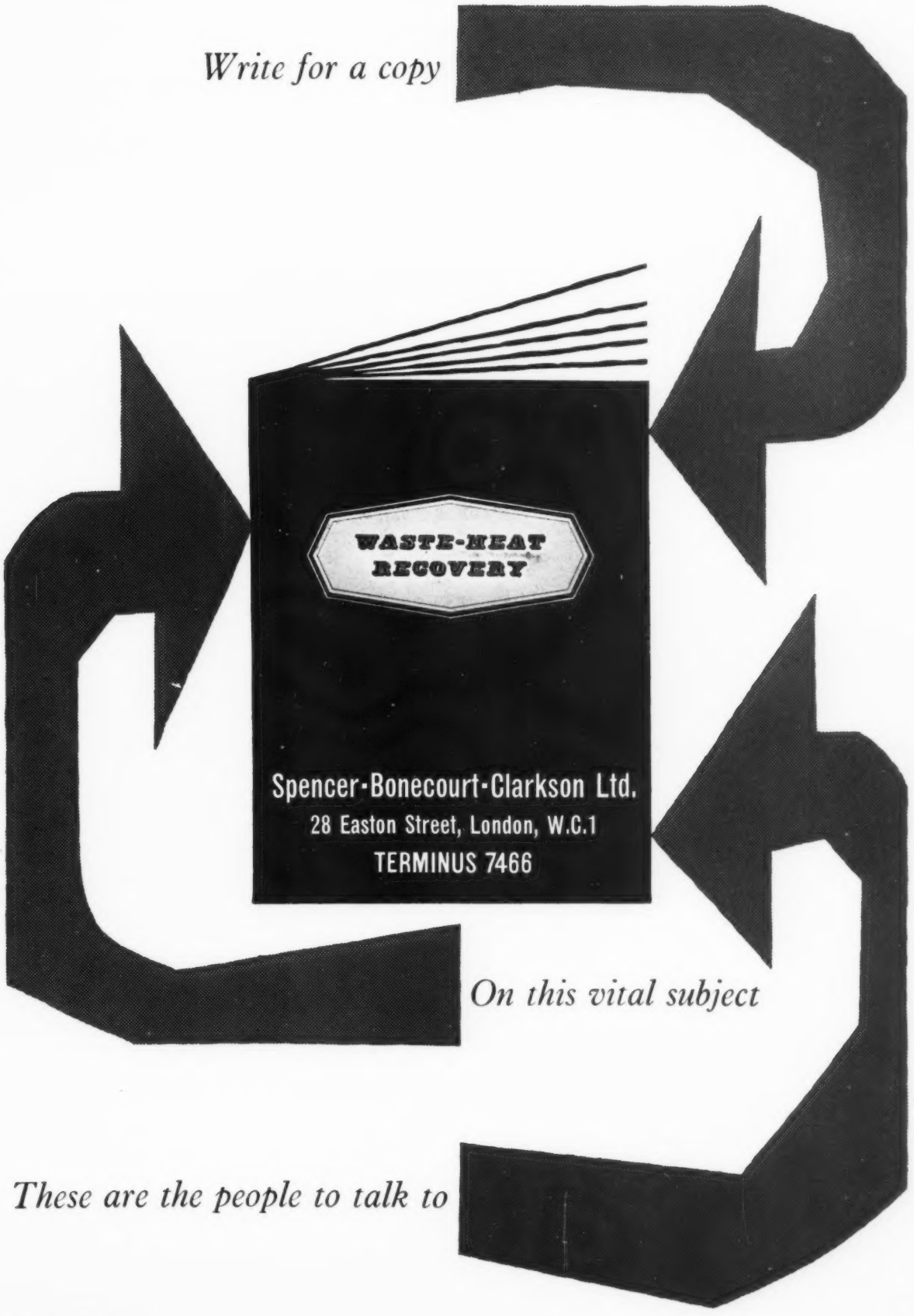

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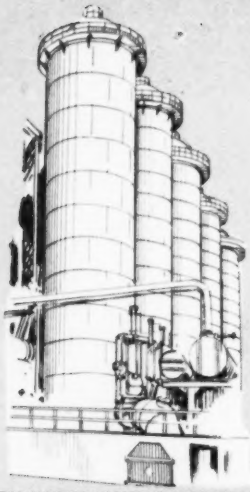
These are the people to talk to



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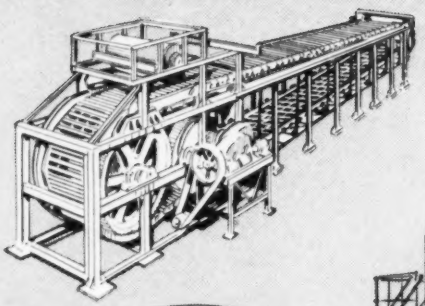

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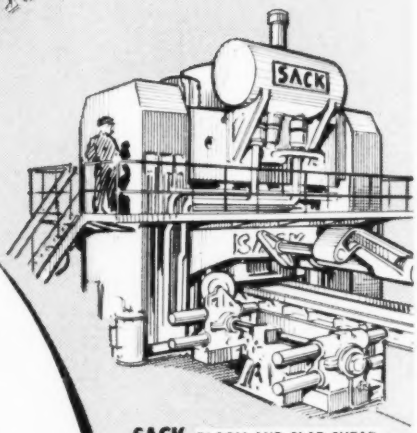

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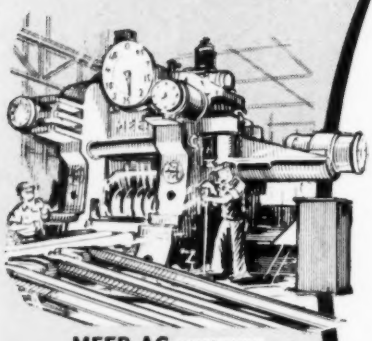

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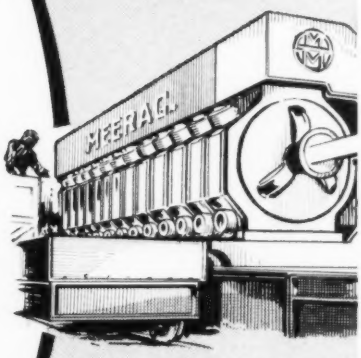

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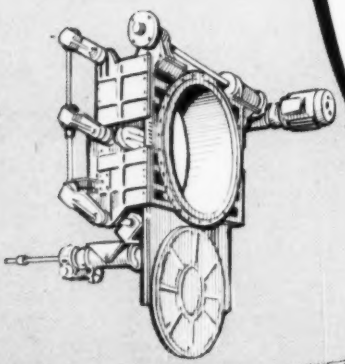

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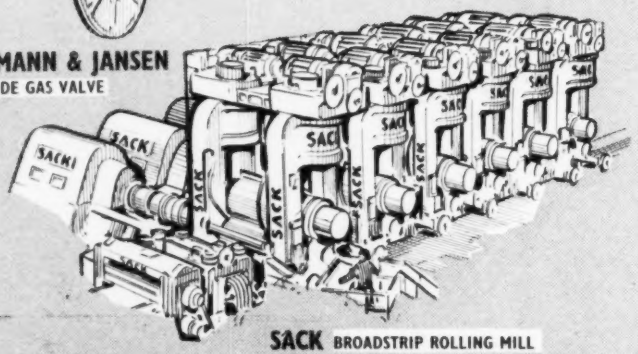

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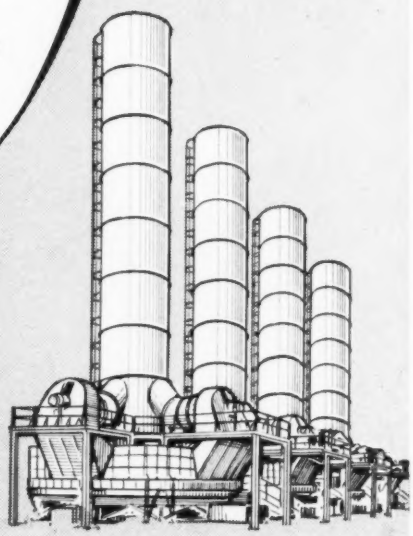

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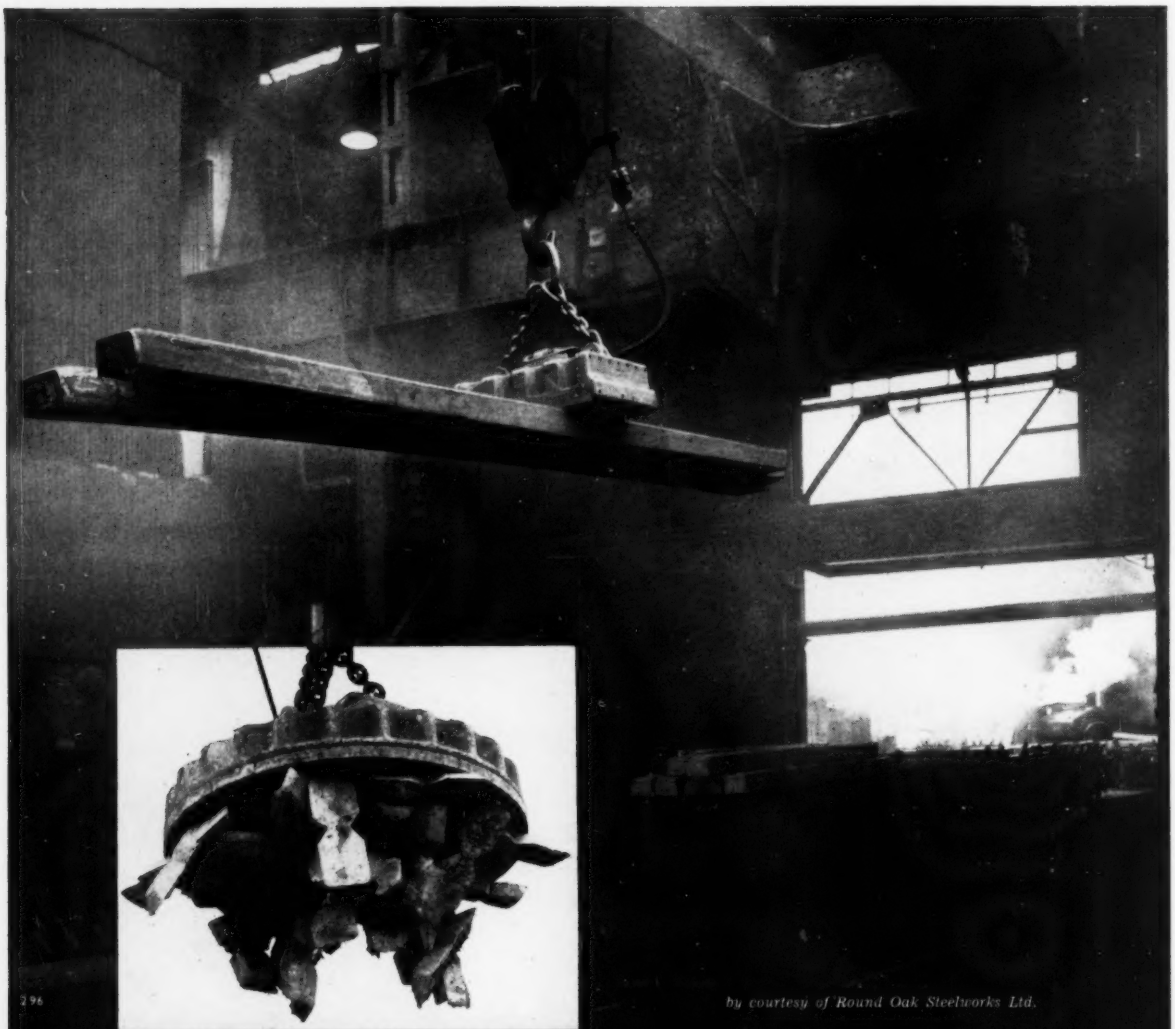

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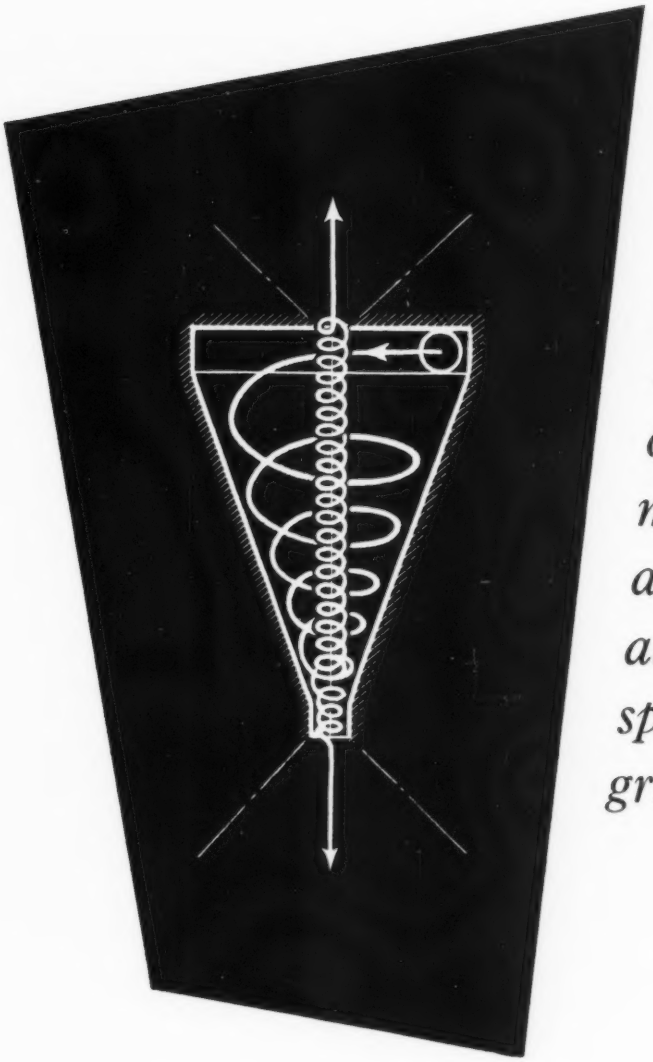

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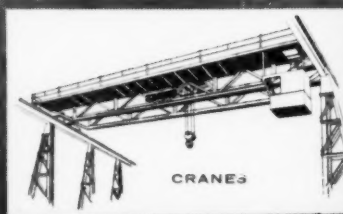

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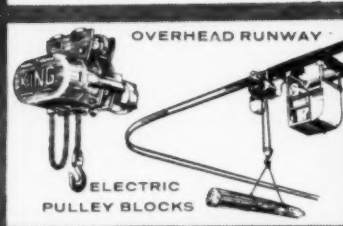

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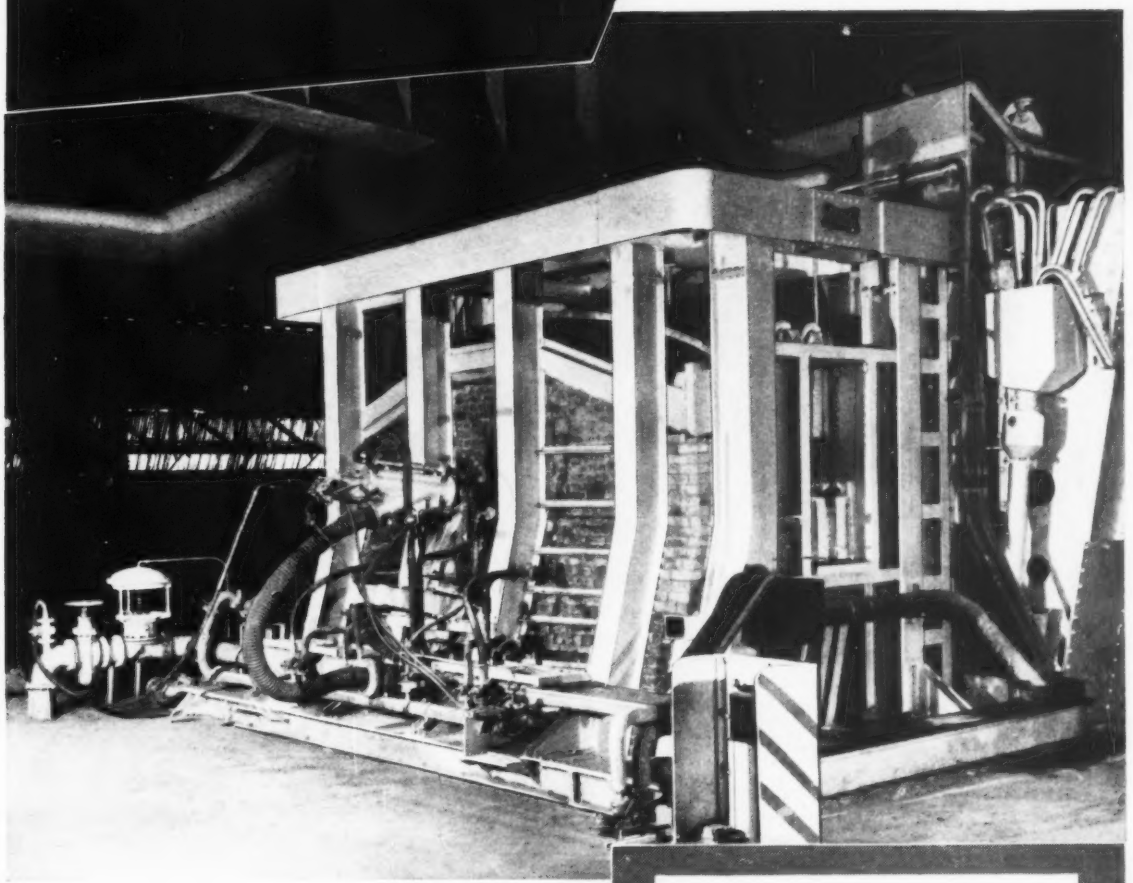

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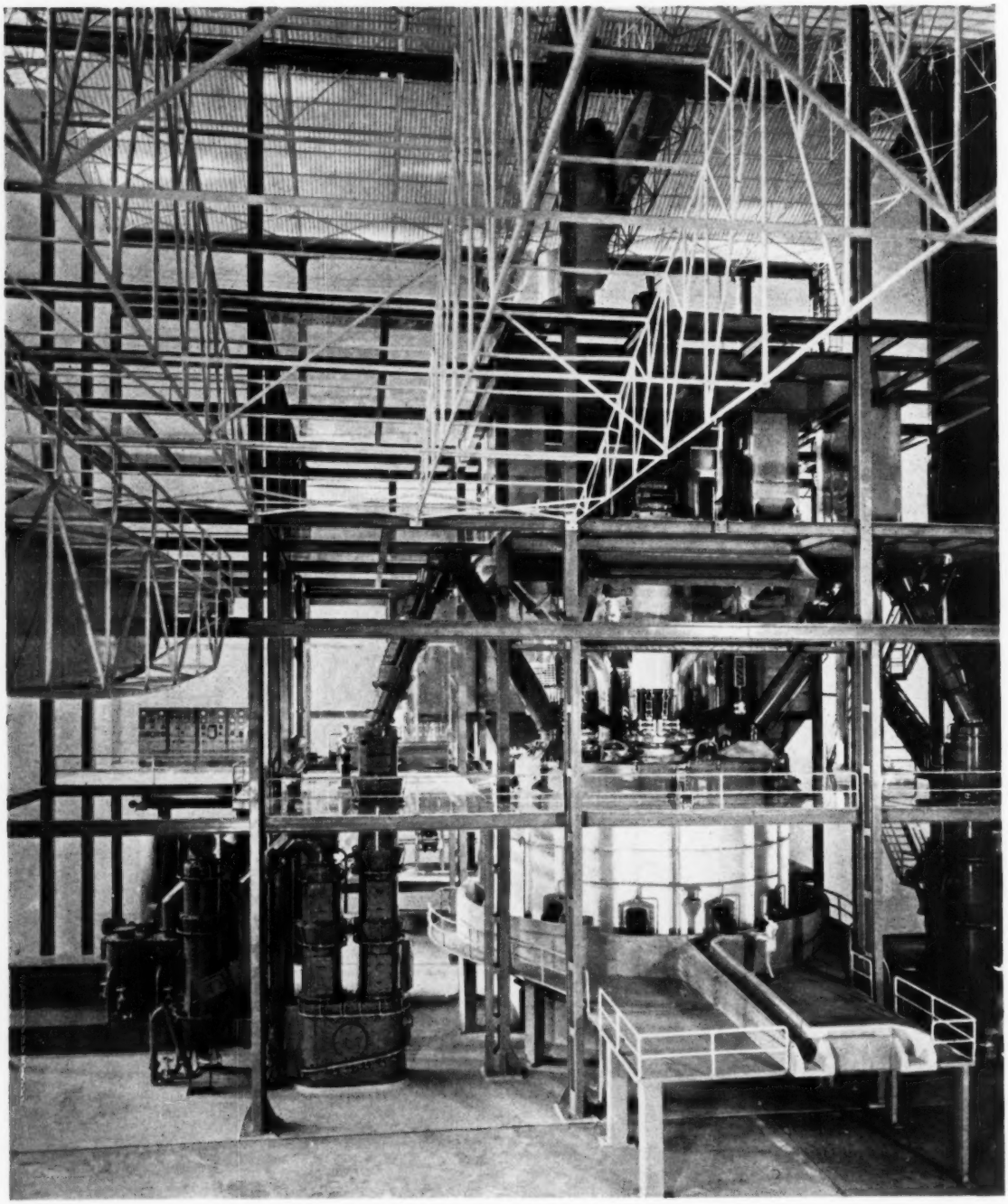

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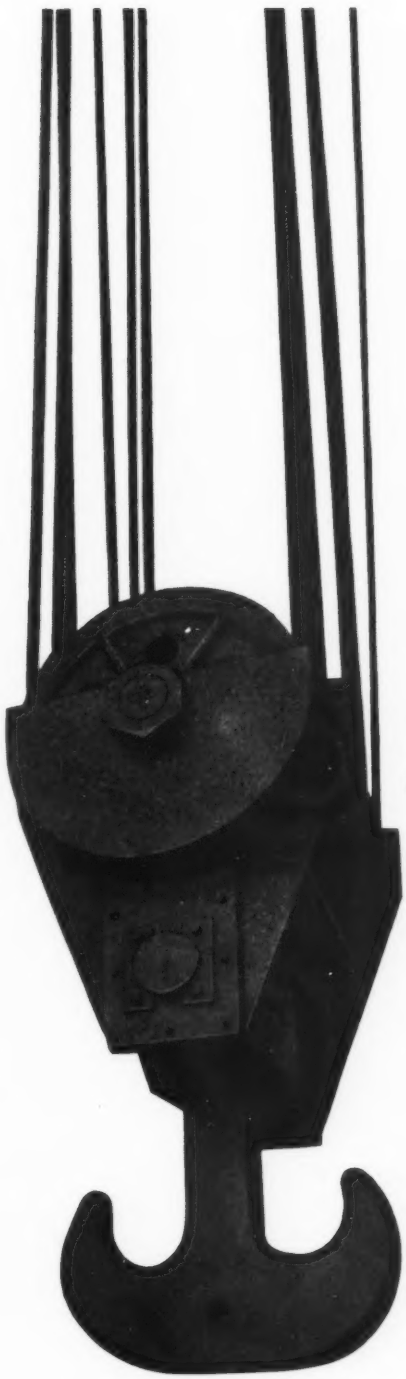

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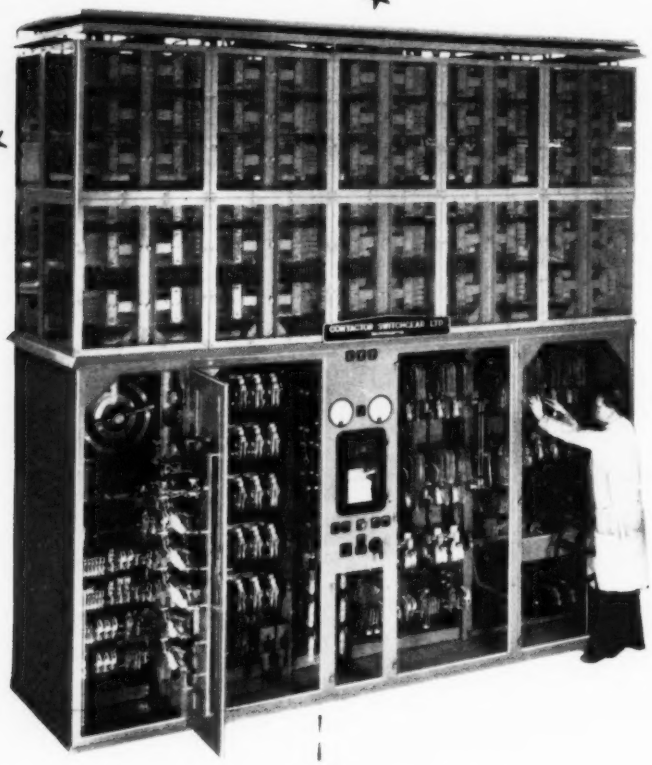

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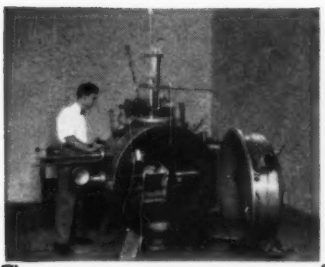

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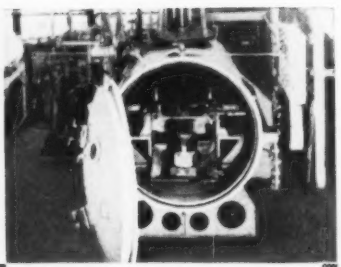

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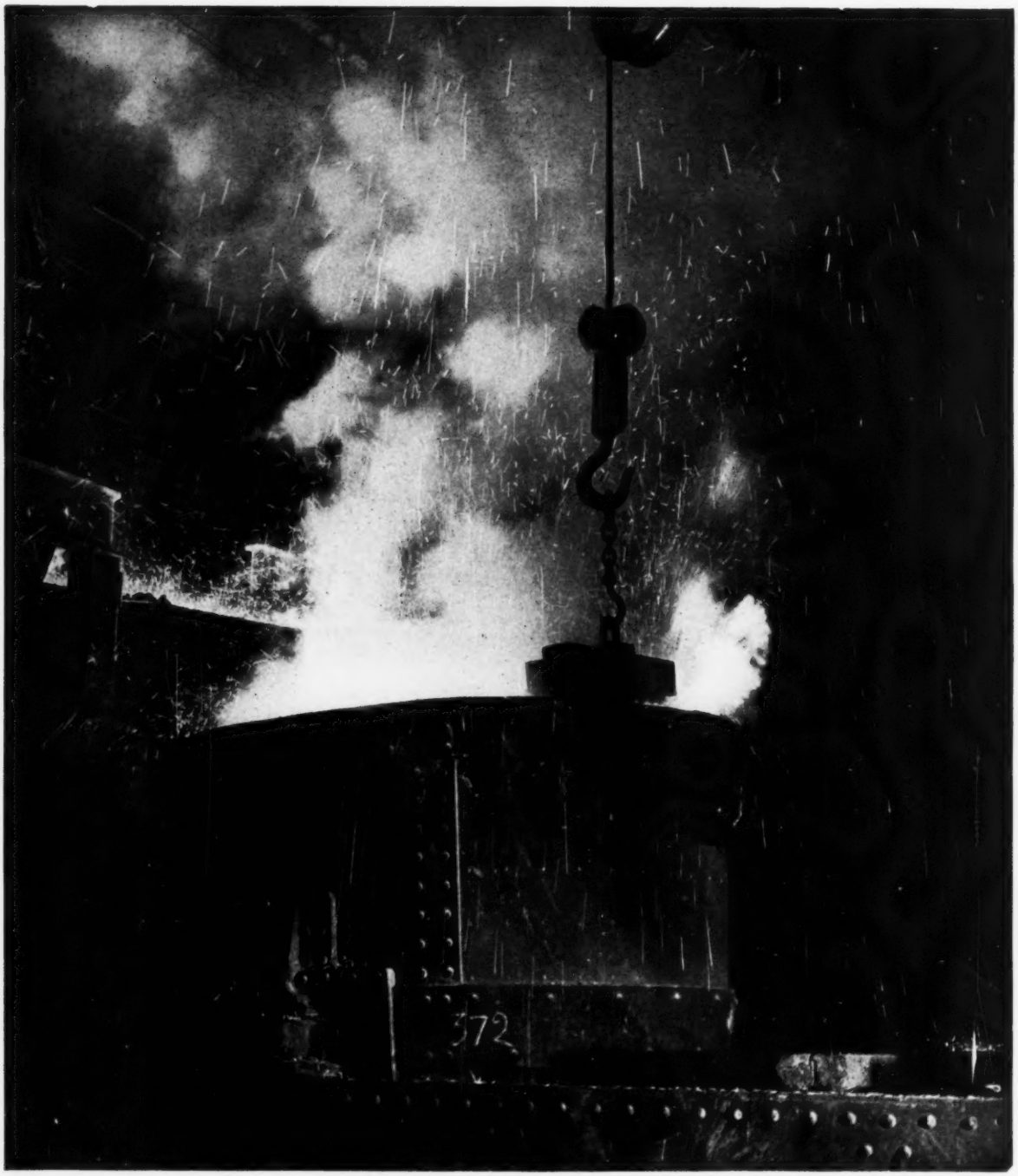

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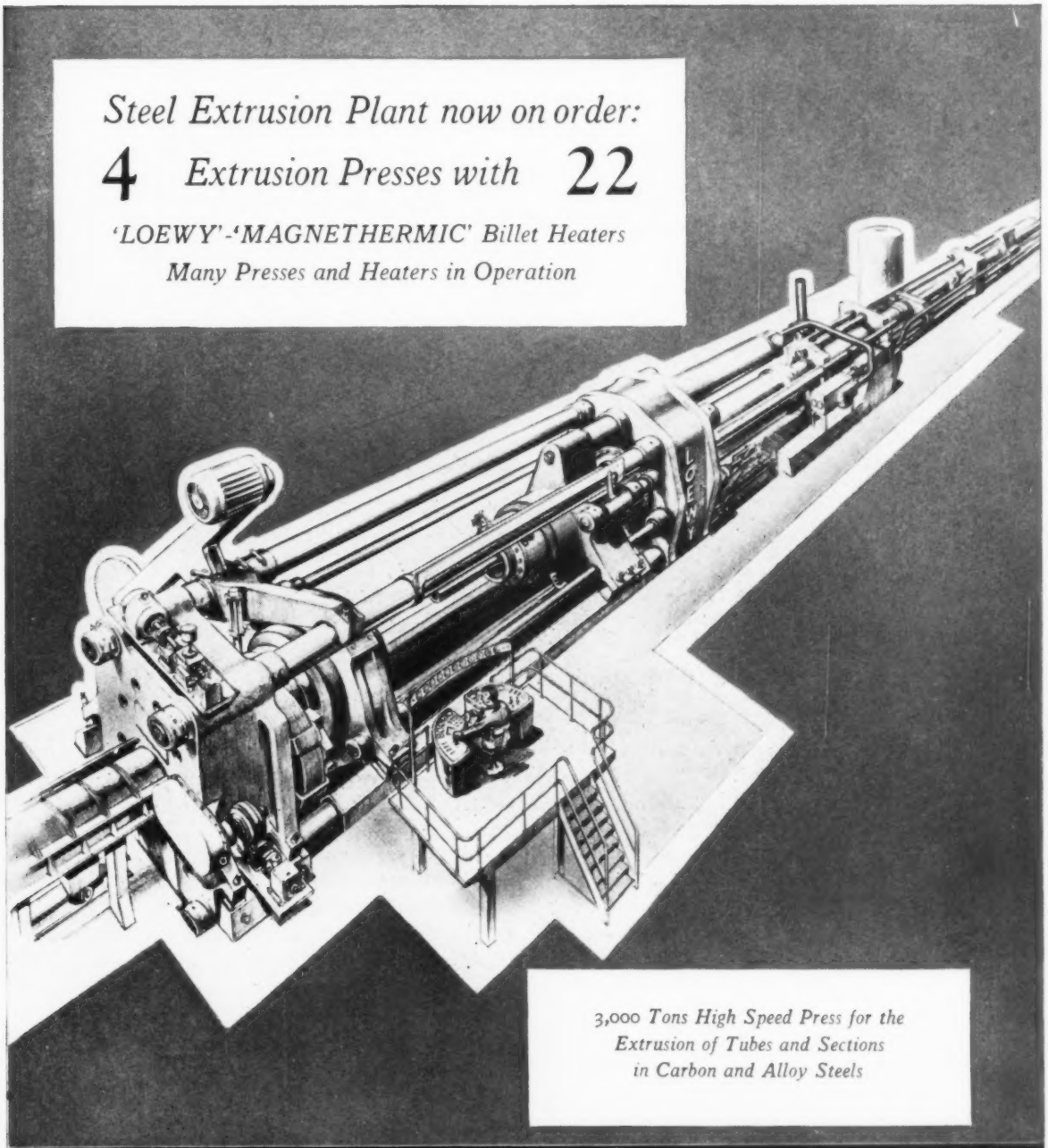

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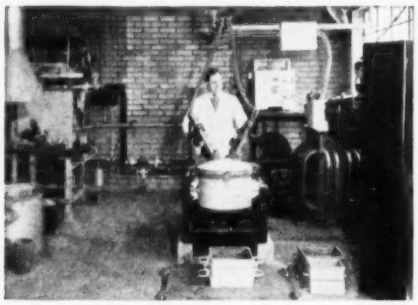

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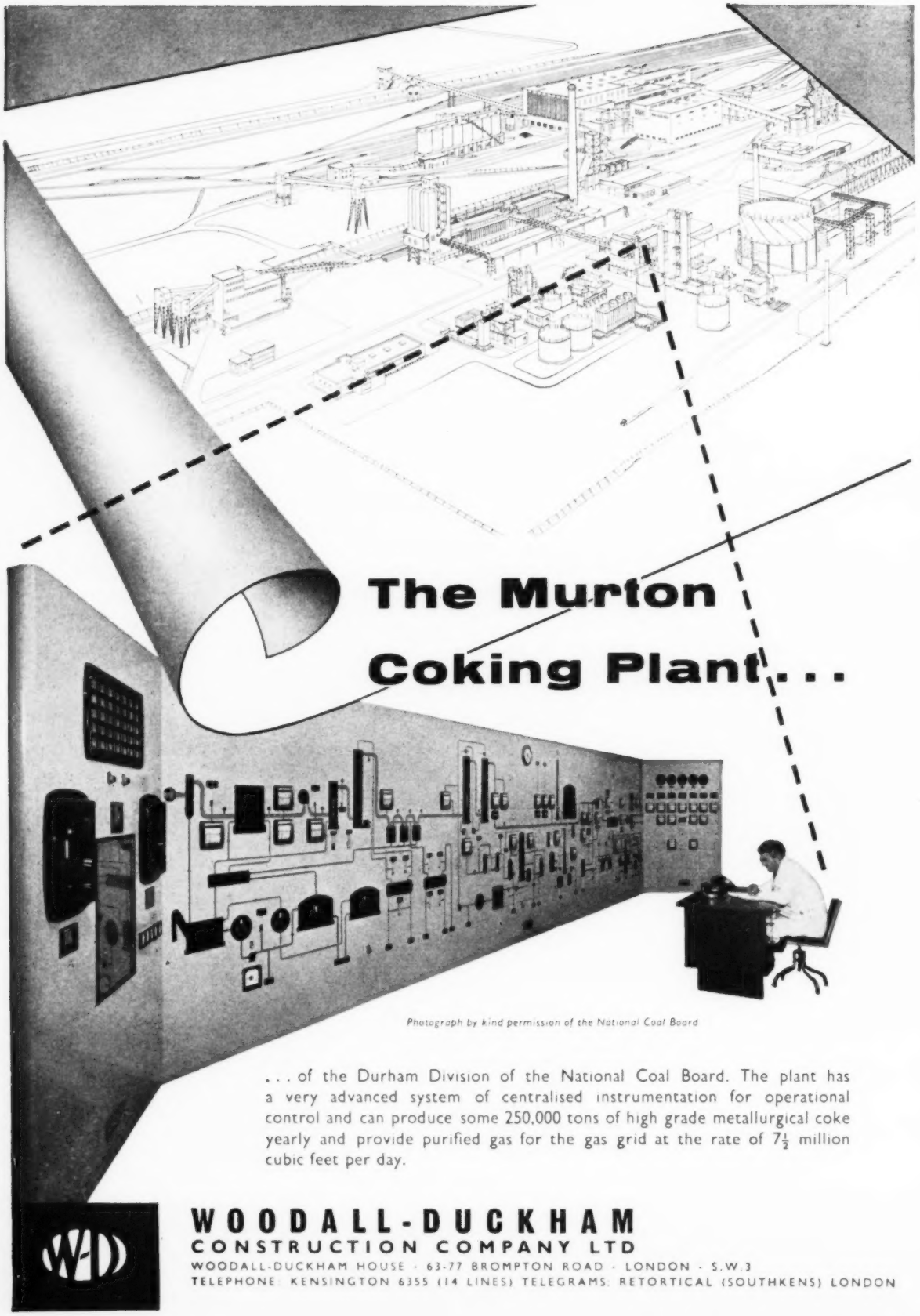

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Journal of The Iron and Steel Institute

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W. C. BELL, F.I.M.

WILLIAM CHARLES BELL was educated at Allan Glen's School and the Royal Technical College, Glasgow. He joined the laboratory staff of Stewarts and Lloyds Ltd at Clydesdale Steelworks in December 1914 but almost immediately joined the army and was on active service in France until severely wounded during the Battle of the Somme one year later. He returned to Clydesdale Works on demobilization and in 1924 he was appointed assistant manager of the open-hearth plant, until in 1929 he transferred to the company's newly formed central research department. He went to the Alfred Hickman branch (now Bilston Iron and Steel Works) of the company in 1930 as works metallurgist to develop technical and operating control of steel supplies to the company's seamless and welded tube mills. In 1936 Mr Bell became assistant general manager at Bilston and shortly afterwards became responsible for the company's Department of Research and Technical Development in the Midlands.

In 1939 he was appointed general manager of the mines and quarries and iron and steelworks at Corby, where he was in charge for most of the war years. He became joint director of the Department of Research and Technical Development in 1944, with special responsibilities for the post-war iron and steel development programme at the company's Bilston, Clydesdale, and Corby works. Mr Bell was elected to the Board of Directors of Stewarts and Lloyds in 1954 on his appointment as Managing Director of Iron and Steel Production.

Mr Bell is a Fellow of The Institution of Metallurgists. He has travelled widely in Europe, Canada, and the USA, and in 1953 he was invited to address the American Iron and Steel Institute, when his review of European operating and technical practices was awarded the first Gary Memorial Medal to be held by a non-American.

Mr Bell has served on the Council of The Institution of Metallurgists and of the British Iron and Steel Research Association and has been a chairman of the Association's Ironmaking Divisional Panel and a member of the Steelmaking Panel. He was recently co-opted to the Council of the British Coke Research Association. He is a member of several committees of the British Iron and Steel Federation, and of the North Midland Resettlement Committee of the Regular Forces Resettlement Service of the Ministry of Labour and National Service.

Mr Bell became a member of The Iron and Steel Institute in 1936 and was elected to the Council in 1955.

W. C. Bell, F.I.M.
Member of Council

The general world iron-ore situation

C. R. Wheeler, C.B.E.

This lecture was delivered by Mr Wheeler at the meeting of the Iron and Steel Engineers Group held in the Netherlands in April 1959, the theme of which was the handling and treatment of iron ores. The proceedings of the meeting have recently been published as No.65 in the Institute's Special Report series. The report consists of 138 quarto pages, bound in cloth with stiff board covers and illustrated with 33 line drawings and 83 photographs. Copies may be obtained from the Secretary, price £3 5s (plus 1s postage). 1765

I AM VERY PLEASED that your Chairman and the Engineers Group have thought fit to choose Holland for the first overseas visit of the Group, and we know that we shall enjoy our stay here. It is also very appropriate to visit Holland in view of the background topic that is to be discussed on this occasion. Rotterdam is perhaps the largest iron-ore port in the world and normally handles a total quantity of ore equivalent to half the total consumption of seaborne ore used by British industry, 8·7 million tons in 1957 and almost 8 million tons in 1958. In addition to that, the works at IJmuiden, which have been developed entirely using seaborne ore, have for a long time had a blast-furnace practice of great interest to all the members of the British iron and steel industry.

In the iron-ore business, in common with the steel business, there has recently been a trade recession. First of all there was a very severe recession in the USA, and later this spread to most of Europe, with the result that since the middle of 1958 shipments of iron ore have been very much below those previously registered. Until 1957 the iron and steel industries of the world were mainly concerned about the source of their iron ore, but the recession has brought a sharp change in that point of view; it is now possible to survey the scene a little more calmly than was possible two years ago.

Against the background of continued demand, great efforts were made in the exploration and development of iron-ore fields, and the work done in the 1950's with regard to both exploration and development led us to feel that we could have a certain feeling of security in the iron and steel industry for a great many years to come. Notable developments that affected both sides of the Atlantic were the well known orefields of Labrador, Venezuela, Liberia, and Peru. Those were primarily developed for the industry in the USA, but at least two of them are of importance to the European industry. The discovery of further iron-ore deposits in Quebec, Labrador, Brazil, Mauretania, and French Equatorial Africa provided an assurance of future supplies that made the world's iron and steel industries feel fairly complacent about the future.

The recent recession in the USA has checked the development that might have been expected, but it is noteworthy that the United States Steel Corporation have thought fit to embark at the lowest point of the recession on a development programme of the big iron-ore deposits on the Hudson River at Cartier. Great interest is also being taken in respect of the deposits at Wabush Lake.

I have already referred to the big deposits in the north of Quebec and Labrador; in the area of Ungava Bay there are great deposits about which there has been much talk in recent times. It will be interesting to learn what will happen in this area, as I believe that Ungava Bay can ship only four months in the year and a project in this inhospitable country must involve a large capital outlay.

There are enormous deposits in this region which will provide great reserves for the future and which will probably be developed.

The areas that may be developed in the comparatively near future and are likely to be of particular interest to the European industry are those along the coast of West Africa. There are three major deposits here which will probably be developed shortly; these are the deposits in Mauretania, which will be shipped from Port Etienne, those in Sierra Leone, and those in Liberia, which seem to be very large and run right up into French Guinea, where also there appear to be very large deposits. There are also two more in that area which will probably be developed to a greater extent than at present, the Bomi hills deposit in Liberia and the Conakry deposits in French Guinea.

So far I have dealt with existing and potential sources of ore that are or will be seaborne. There are great deposits of iron ore in the Far East, some of which have been utilized by Europeans; India, and particularly Goa, has considerable deposits, and we have had experience in Britain of the use of Malayan ore. It seems to me that there will be a tendency for

these ores to go to Japan, but we in Europe have from time to time made use of them.

I have referred so far only to the developing and newly developed sources of ore that are available to the USA and to Europe; however, it is worth while briefly to consider the traditional European sources. The major source is Sweden; here plans are laid for further substantial development, and I have no doubt whatever that Swedish iron ore will continue to retain its very important place in European supplies. There is a relatively small supply from Sweden to the USA, but this is not likely to increase to an important extent.

There are also a large number of sources lying round the Mediterranean, in Spain, Morocco, and the whole North African coast. It is unlikely that increasing supplies will be obtained from those sources. They have been in the past a very important and stable source of supply, but it is improbable that there will be any very large developments there. There is, however, one major orefield in the North African region, not far from the Mediterranean, which is of particular importance in another connection. Much has been spoken and written about the large deposit at Tindouf, in the Atlas Mountains. It seems to me unreasonable to expect early development there, but one important fact about the Tindouf deposits should be mentioned. All the new deposits about which I have been speaking up to now are low-phosphorus ores, whereas Tindouf is a moderately phosphoric ore. Looking over the general field with which I have dealt, Tindouf is the only source of phosphoric ore, other than the Swedish development in view. It is fortunate for the European industry that the recent developments in pneumatic steelmaking processes make phosphorus less important than it used to be, in view of the emphasis on low-phosphorus ores in all the new sources that are being investigated and developed.

Sintering and pelletizing are matters of great interest to the iron and steel industry. We are quite properly looking increasingly for fine ores, both the product of concentrates and the product of run-of-mine. It looks as though the very forward-looking policy of the industry, not only in Britain but also on the continent of Europe, will prove to be soundly based, because a number of the deposits of which I have spoken are either in fine form naturally or require concentration. In due course, no doubt, there will be many discussions at meetings of the Institute and elsewhere on the respective merits of sintering and pelletizing for some of these fine ores. The present tendency is towards sintering, but with the development of some of the ores requiring concentration pelletizing will no doubt become more important.

When seaborne iron ore is under discussion, a primary consideration is shipping, a matter to which everyone in the iron-ore business has given a great deal of thought in recent years. During this visit we shall be able to see a further development in the handling of big ships. Whether in a period of good trade or in a period of recession, the basic costs of shipping will remain the same; although with the present reduced shipments of ore there is a slump in shipping, and in Britain a number of ore carriers have been laid up, it is the cost of moving the ore that matters most.

A recent tendency has been to build carriers designed especially to suit the job. In this branch of our activities perhaps one of the most important developments of the last ten years has been the concentration of attention on both size and type of ship. There has been a good deal of controversy on the question of size, and a great deal of interest is now being taken in really large carriers. In the long term I am confident that a great deal of ore will be moved in very large carriers.

The industries that are dependent on the port of Rotterdam are extremely fortunate in this regard, because the port of Rotterdam is being extremely forward-looking. It only remains for those who use Rotterdam to get a few more iron-ore properties developed where the ships can be loaded. In Britain, unfortunately, we are hampered by rather shallow waters round our coasts and considerable draught limitations in our ports. We have been trying to deal with this problem as efficiently as possible by building ships up to the limit of many of our ports. The future holds the possibility, if not the probability, of our having to give serious consideration to transshipment.

Where are we going to be able to load ships of 45000–60000 tons? There are only two schemes in prospect at the moment that will readily enable such ships to be loaded, in Brazil and Mauretania. The future development of a policy of transshipment, as at Rotterdam, seems to be most promising, given the ability to load these big ships. In the UK the Steel Company of Wales and my own company (GKN) have a scheme at Milford Haven that will enable us to handle really large ships, with transshipment to the works in the west. It may be that in the future a considerable number of blast-furnaces in the UK will be supplied through Milford Haven from the west, and from the east through Rotterdam. It is highly probable that within the next year or so there will be a good deal of transshipment through the port of Rotterdam to British east coast plants.

I have given you a broad discourse on the ore situation. I have emphasized shipping, which I believe to be the most important economic factor in the movement of iron ore. It is fairly simple to judge the places from which we shall get our ore, and you engineers will know how cheaply it can be put into a ship, but the technique of handling it from then on is of very great importance to us in the iron and steel industry, and those of us who have to deal with the cost aspects are interested in the papers to be presented to this meeting.

Some factors affecting fume evolution from molten steel during oxygen injection

K. Knaggs and J. M. Slater

SYNOPSIS

The results of experiments on fume generation obtained by the injection of oxygen into 350-lb steel melts are reported. It has been found that, in iron–carbon melts the amount of fume evolved increases with the carbon content of the melt and the oxygen input rate. Injection of air drastically reduces the quantity of fume produced but also seriously lessens the rate of carbon removal. Injection of oxygen–air mixtures gives no improvement over oxygen alone.

The results of tests on iron–silicon–manganese–carbon melts show anomalous effects and it is suggested that the depths of submersion of the oxygen lance tip during injection may be responsible for such contradictions.

Fume samples have been collected simultaneously from within the furnace and the extraction duct. These samples have been subjected to X-ray analysis which indicates that the fume when first formed consists of elemental iron.

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A SERIES of experiments has been made in conjunction with other work on the efficiency of fume collecting systems to find out what steelmaking variables affected the quantity of fume produced during the oxygen injection period of foundry steelmaking in electric furnaces. It was thought that any results so obtained would fill the gap between small-scale laboratory experiments and practical works investigations and help to throw some light on the more general problem concerning the mechanism of fume formation.

EXPERIMENTAL PROCEDURE

All the experiments were carried out in a carbon resistor rod furnace on heats weighing about 350 lb using a normal oxygen injection technique; that is, by lancing with a $\frac{3}{8}$-in. dia. pipe just below the slag/metal interface.

The furnace used had no openings in the roof and thus the fume generated was emitted through the door from whence it was extracted via a hood and duct arrangement placed above the furnace door as illustrated in Fig.1. The fume in the duct was sampled by an isokinetic method using a simplified version of the apparatus developed by BISRA.[1] The apparatus used by BSCRA is shown diagrammatically in Fig.2. The probe is made of stainless-steel tubing with a screwed-on knife-edge nozzle of $\frac{1}{2}$ in. dia. and is faced upstream during sampling. The tube is connected to a filter holder in which a Soxhlet thimble is mounted. In a short pipe on the outlet side of the filter holder a constant-flow orifice is inserted and a differential pressure gauge is connected to the tubes on both sides of the orifice. Gas samples are extracted by a simple air ejector which is connected to a compressed air line. The requisite constant-flow orifice is that which provides isokinetic conditions at the nozzle entrance, that is, it gives a velocity in the sampling nozzle equal to the velocity of the gases in the duct, this velocity having been previously determined by inserting a pitot tube or a velometer near to the point of sampling. Tests conducted as a preliminary to this work have shown that the Soxhlet thimble used in the apparatus is over 95% efficient for the collection of fume particles and, for the duration of the sampling period, retains this efficiency with a negligible increase of the differential pressure across the filter. The quantity of fume collected was 0·2% or more accurately 1/506th of the total amount of fume evolved.

To obtain results that were fairly representative of a given metal composition, the oxygen injection period was kept to a minimum and was of 36–45 s duration in nearly all cases. The fume was sampled for a 30-s period in the middle of this injection time. A sample of metal was taken for analysis immediately before and after oxygen injection and the bath temperature was recorded immediately before injection.

The following remarks and data summarize the results obtained.

INJECTION OF OXYGEN INTO IRON–CARBON MELTS

In the first instance, work was conducted on relatively pure iron–carbon melts, i.e. the silicon content was below 0·02%Si and the manganese content was always

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1 *Method of extracting fume by means of a hood and duct*

below 0·25%Mn and usually below 0·15%Mn. In such heats the oxidation of carbon (denoted by the appearance of the carbon flame) commences as soon as oxygen injection starts.

All the results for this series of heats are shown graphically in Fig.3, the lines drawn thereon having no intentional statistical significance but serving only to delineate the various ranges of oxygen flow rate. From this figure it can be concluded that:

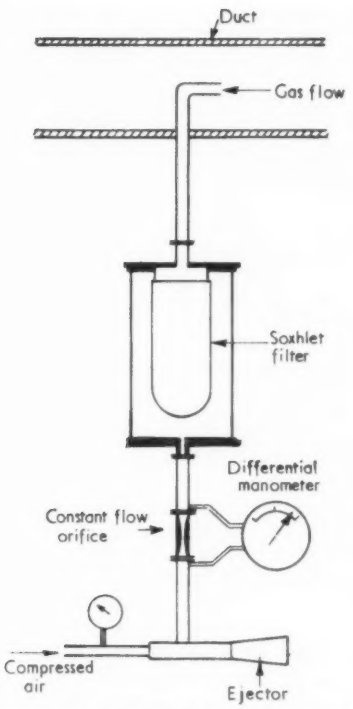
(i) for a fixed mean carbon level, i.e. the average of the initial and final carbon contents, the amount of fume increases as the oxygen flow rate increases. The results given in Table I show this for two mean carbon levels

(ii) for a fixed flow rate the amount of fume increases with increasing mean carbon content. Table II gives a representative sample of results to justify this conclusion for flow rates of 32–35 ft³/min, this being the range of flow rate at which most of the heats were injected with oxygen since it was a range which gave an appreciable quantity of fume without causing undue damage to the furnace.

Since the chief reaction taking place in such melts is the oxidation of carbon, it is possible to relate the weight of fume collected per minute to the rate of carbon removal in lb/min. Such a relationship for four different mean carbon levels is shown graphically in

TABLE I Results showing the relationship between the quantity of fume collected and the rate of oxygen injection for two levels of mean carbon content

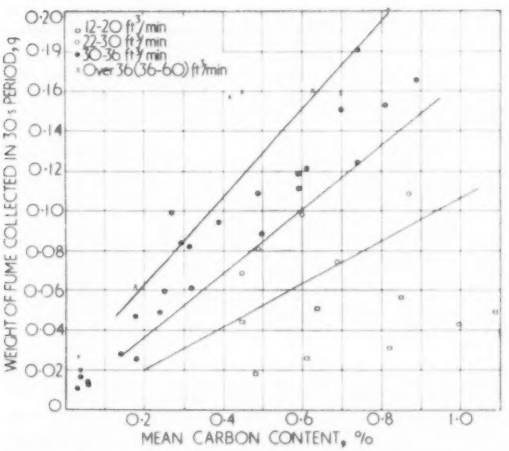
Heat no.	Charge wt, lb	Oxygen injection time, s	Temperature of metal before injection, °C	Metal analyses, % (a = before injection, b = after injection)			Rate of carbon removal, lb/min	Rate of oxygen injection, ft³/min	Mean carbon content, %	Wt of fume collected in a 30-s period, g
				C	Si	Mn				
1*E*	390	36	1490	*a* 0·62	0·003	0·07	2·16	51·0	0·45	0·1595
				b 0·29	0·003	0·07				
5*B*	350	40	1500	*a* 0·58	0·013	0·14	1·56	32·3	0·43	0·1221
				b 0·29	0·010	0·11				
2*B*	370	44	1510	*a* 0·54	0·006	0·08	1·02	24·0	0·45	0·0685
				b 0·36	0·005	0·08				
7*A*	350	46	1520	*a* 0·54	0·04	0·25	0·72	18·0	0·46	0·0447
				b 0·38	0·02	0·13				
6*K*	300	40	1490	*a* 0·54	0·03	0·07	0·60	13·6	0·48	0·0188
				b 0·41	0·01	0·06				
1*C*	370	35	1500	*a* 0·76	0·009	0·12	1·62	43·5	0·63	0·1600
				b 0·49	0·009	0·11				
3*C*	360	45	1485	*a* 0·72	0·03	0·12	1·02	35·7	0·61	0·1217
				b 0·50	nil	0·09				
1*B*	360	37	1500	*a* 0·72	0·006	0·29	0·78	25·3	0·65	0·0663
				b 0·59	0·006	0·14				
6*J*	300	38	1480	*a* 0·68	0·01	0·07	0·54	13·6	0·61	0·0261
				b 0·57	0·01	0·06				



2 *Arrangement used by BSCRA for sampling fumes during the oxygen-injection period of steelmaking*

Fig.4. In heats such as those under consideration this is a more satisfactory way of plotting the results since it indicates that the vigour of the carbon/oxygen reaction is the factor affecting the quantity of fume generated in such melts whilst a plot relating mean carbon content, weight of fume collected, and oxygen flow rate takes no account of the fact that at high specific oxygen input* levels of over 150 the efficiency of oxygen usage for carbon removal varies appreciably even for heats of the same carbon content.

* Specific Oxygen Input (SOI) = $\frac{\text{Vol. of } O_2 \text{ blown (ft}^3\text{)}}{\text{Wt of metal (tons)} \times \text{time of blow (min)}}$



3 *Graph showing the relationship between the mean carbon content (i.e. the average of the initial and final contents) and the weight of fume collected for various ranges of oxygen*

INJECTION OF AIR AND AIR–OXYGEN MIXTURES INTO IRON–CARBON MELTS

Some of the results from this series of experiments are given in Table III. It can be seen from a comparison of Table III with Tables I and II that when air alone was injected into the steel there was very little fume generated even when the flow rate was exceptionally high. Unfortunately, very little carbon removal took place. It was thought that a compromise between carbon removal rate and fume generation might be struck by using oxygen–air mixtures. The results given in Table III show that this hope was not fulfilled and that the amount of fume produced by such mixtures was similar to that obtained when an equivalent quantity of pure oxygen was injected although the quantity of fume generated seemed to vary more arbitrarily than when injecting with oxygen alone.

INJECTION OF OXYGEN INTO Fe–Si–Mn–C MELTS

Some results from a series of Fe–Si–Mn–C melts having silicon contents below 0·6% are presented in Table IV. By a comparison of heats 4*A*, 4*B* with 4*C* and 6*D*, 6*E* with 6*F* it can be seen that fume is produced before the carbon reaction commences, but the amount of

TABLE II Results showing the relationship between the quantity of fume collected and the mean carbon contents for oxygen flow rates within the range 32–35 ft³/min

Heat no.	Charge wt, lb	Oxygen injection time, s	Temperature of metal before injection, °C	Metal analyses, % *a* = before injection, *b* = after injection C	Si	Mn	Rate of carbon removal, lb/min	Rate of oxygen injection, ft³/min	Mean carbon content, %	Wt of fume collected in a 30-s period, g
3*A*	350	40	1500	*a* 1·01	0·02	0·21	1·31	33·0	0·89	0·1645
				b 0·76	0·02	0·16				
2*C*	370	36	1495	*a* 0·92	0·004	0·12	1·38	34·3	0·81	0·1529
				b 0·70	0·004	0·12				
3*B*	350	42	1480	*a* 0·86	0·01	0·15	1·15	34·2	0·74	0·1243
				b 0·63	0·01	0·12				
5*B*	350	43	1500	*a* 0·69	0·03	0·23	0·98	34·8	0·59	0·1108
				b 0·49	0·01	0·16				
3*D*	360	41	1505	*a* 0·44	nil	0·10	1·30	34·4	0·32	0·0609
				b 0·20	nil	0·08				
3*E*	370	43	1500	*a* 0·23	nil	0·10	0·87	32·0	0·14	0·0277
				b 0·06	nil	0·06				
3*G*	370	44	1530	*a* 0·03	0·02	0·02	nil	33·7	0·03	0·0103
				b 0·03	0·02	0·02				

TABLE III Some typical results obtained when injecting air and air–oxygen mixtures

Heat no.	Charge wt, lb	Oxygen injection time, s	Temperature of metal before injection, °C	Metal analyses, % *a* = before injection, *b* = after injection C	Si	Mn	Rate of carbon removal, lb/min	Rate of oxygen–air injection, ft³/min	Mean carbon content, %	Wt of fume collected in a 30-s period, g
8*A*	350	38	1580	*a* 0·71	0·08	0·46	...	41 Air	0·71	0·0369
				b 0·71	0·04	0·38				
8*B*	350	32	1600	*a* 0·70	0·02	0·32	...	44 Air	0·71	0·0256
				b 0·72	0·01	0·26				
8*C*	350	45	1590	*a* 0·73	0·01	0·26	...	106 Air	0·72	0·0233
				b 0·72	0·01	0·22				
9*A*	350	47	1610	*a* 1·22	0·18	0·65	0·76	34 Oxygen	1·13	0·2077
				b 1·05	nil	0·43		30 Air		
9*D*	350	46	1515	*a* 1·11	0·03	0·28	0·92	19·6 Oxygen	1·01	0·1210
				b 0·91	nil	0·22		19·8 Air		
10*A*	350	46	1560	*a* 0·67	0·11	0·38	0·42	26·5 Oxygen	0·62	0·1119
				b 0·58	nil	0·26		19·6 Air		
10*B*	350	45	1605	*a* 0·58	0·04	0·25	0·70	12·6 Oxygen	0·51	0·0485
				b 0·43	0·04	0·20		23·3 Air		

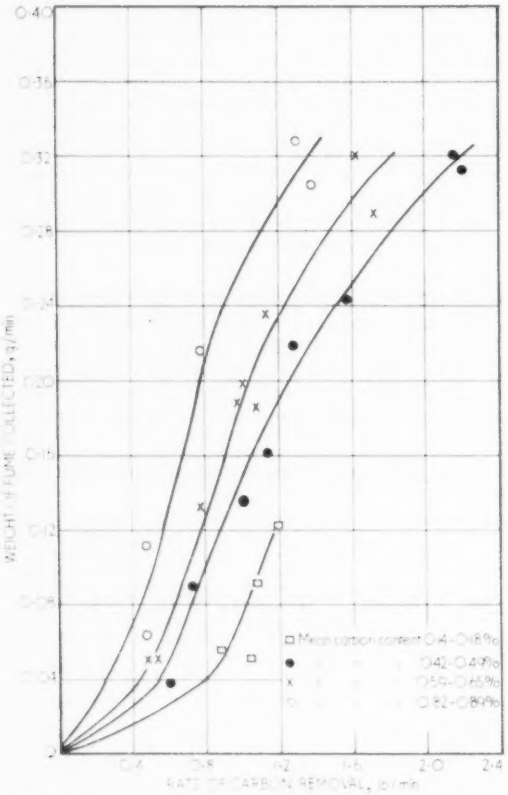
fume increases markedly when the silicon and manganese have been reduced sufficiently to allow the carbon reaction to start, provided the initial carbon concentration is above 0·25% (cf. 4*F* and 4*H*).

A further series of Fe–Si–Mn–C melts which had higher initial silicon contents were studied. The results of these heats are given in Table V, from which it can be seen that, although no carbon removal is taking place in heats 11*A*, *B*, *E*, and *F* the fume generated from 11*A* and *B*, where the carbon concentration is high, is much greater than that generated from 11*E* and *F*, where the carbon concentration is low. It seems, therefore, that under such conditions the presence of carbon and not necessarily its removal has a marked effect on the quantity of fume produced.

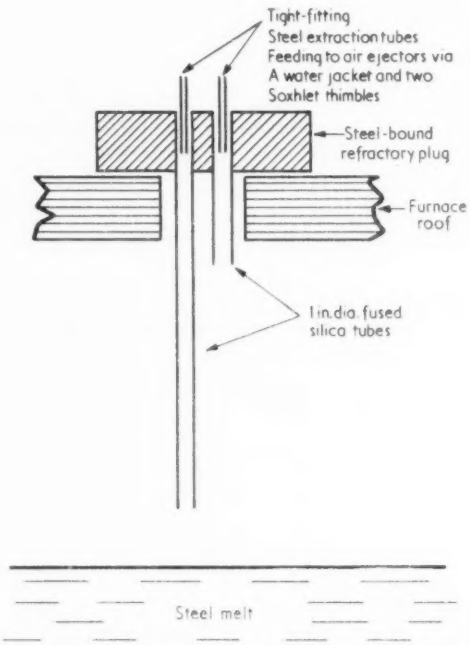
Furthermore, a comparison between the results given in Table IV and Table V indicates that at high silicon and carbon levels of over 1% the quantity of fume generated is much greater than from silicon levels of 0·5–0·7%.

SAMPLING THE FUME WITHIN THE FURNACE BODY

A twin-probe sampling device was constructed which could be lowered into the furnace through a hole that was purposely made in the furnace roof. The object was to sample the fume generated, simultaneously in the duct and at two levels within the furnace, one about 3 in. above the slag surface and the other some 15 in. higher, so that any chemical changes in the fume



4 *Graph showing the relationship between the rate of carbon removal and the quantity of fume collected per minute for four levels of mean carbon content*



5 *Arrangement used for sampling the fume from within the furnace body*

TABLE IV Some typical results for iron silicon carbon melts

Heat no.	Charge wt, lb	Oxygen injection time, s	Temperature of metal before injection, °C	Metal analyses, % *a* = before injection *b* = after injection C	Si	Mn	Time from start of blow to appearance of carbon flame, s	Rate of oxygen injection, ft³/min	Wt of fume collected in a 30-s period, g
4*A*	300	40	1505	*a* 0·75	0·56	1·19	No carbon flame	34·6	0·0471
				b 0·76	0·30	0·87			
4*B*	325	39	1515	*a* 0·74	0·23	0·84	29	30·8	0·0443
				b 0·70	0·04	0·47			
4*C*	350	43	1500	*a* 0·69	0·03	0·23	3	34·8	0·1108
				b 0·49	0·01	0·16			
6*D*	325	43	1490	*a* 1·19	0·52	0·56	No carbon flame	13·5	0·0174
				b 1·21	0·43	0·47			
6*E*	325	40	1515	*a* 1·19	0·20	0·37	No carbon flame	13·6	0·0189
				b 1·22	0·13	0·30			
6*G*	300	39	1495	*a* 1·14	0·01	0·10	0	13·6	0·0409
				b 1·04	0·01	0·10			
4*F*	350	42	1500	*a* 0·15	0·56	1·15	No carbon flame	35·0	0·0312
				b 0·16	0·26	0·69			
4*H*	400	45	1505	*a* 0·17	0·01	0·18	9	33·3	0·0314
				b 0·06	0·01	0·10			

occurring within the furnace or from furnace to duct could be assessed. A sketch of the experimental arrangement is given in Fig.5.

The samples collected in the above way were subjected to X-ray analysis. These X-ray analyses showed some variation from blow to blow probably due to variation in the atmosphere within the furnace. The general conclusions to be drawn from a study of all the X-ray analyses are that:

(i) the samples collected from the two different levels within the furnace showed no appreciable difference in X-ray analysis

(ii) the samples collected within the furnace consisted mainly of elemental iron and ferrous oxide whilst those collected in the duct were almost wholly Fe_3O_4.

Using the same sampling arrangement one heat was injected with oxygen and at the same time nitrogen was blown into the furnace above the bath in an attempt to keep the atmosphere relatively non-oxidizing. The X-ray analyses of the fume samples collected from this heat showed that the furnace samples consisted almost completely of elemental iron. Analysis of the duct sample showed it to contain mainly Fe_3O_4.

These results indicate that the fume when first formed consists of elemental iron which is converted to oxide during transit to the duct.

DISCUSSION AND CONCLUSIONS

The work summarized in this paper covers a range of working conditions that is too narrow to allow a general appraisal of the mechanism of fume formation. However, the following conclusions will add to the sum total of knowledge on this subject and give some indication of practical steps which might be taken to limit the amount of fume formed.

(1) Under the conditions prevalent in the present experiments, for heats of steel with silicon contents less than 0·6%, the heaviest fume is produced when the carbon dissolved in the iron is being oxidized. It is possible to suggest that in this case one of two things may happen:

(i) a gaseous Fe–C–O complex is formed momentarily which then breaks down to give gaseous iron which condenses, oxidizes, and thus forms fume. Such a theory was first suggested by Kohlmeyer and co-workers[2,3]

(ii) iron present as vapour or as minute particles within or upon the surface of the carbon monoxide bubbles is released when the bubbles burst, thrown above the metal surface by the violence of the bursting, and oxidized by the furnace atmosphere to form fume.

These results indicate that the amount of fume generated in unit time can be decreased by lowering the oxygen input rate and keeping the initial carbon concentration as low as possible commensurate with an adequate boil.

(2) The X-ray analyses of the fume samples collected from within the furnace indicate that the fume when first formed consists of elemental iron which is converted to iron oxides by the prevailing oxidizing atmosphere.

TABLE V A series of results for iron silicon carbon melts to demonstrate the difference between high silicon high carbon melts and high silicon low carbon melts

Heat no.	Charge wt, lb	Oxygen injection time, s	Temperature of metal before injection, °C	Metal analyses, % *a* = before injection *b* = after injection C	Si	Mn	Time from start of blow to appearance of carbon flame, s	Rate of oxygen injection, ft³/min	Wt of fume collected in a 30-s period, g
11*A*	375	46	1500	*a* 1·06	1·27	0·89	No carbon flame	32·8	0·2027
				b 1·06	0·86	0·78			
11*B*	375	35	1505	*a* 1·03	0·77	0·58	No carbon flame	32·5	0·1086
				b 1·01	0·40	0·46			
11*C*	375	50	1500	*a* 0·98	0·23	0·48	29	29	0·0832
				b 0·92	0·02	0·22			
11*E*	375	40	1500	*a* 0·24	1·61	0·21	No carbon flame	35	0·0624
				b 0·24	0·91	0·16			
11*F*	375	51	1490	*a* 0·23	0·73	0·21	No carbon flame	31	0·0435
				b 0·23	0·37	0·08			

(3) The results on melts containing silicon are too few and too mutually contradictory to allow any definite conclusion to be reached. They neither confirm nor deny the theory of Kosmider and co-workers[4] who suggest that fume generation is caused by the thermal evaporation of iron produced by the local high temperatures provided by oxygen injection.

It may well be, however, that in silicon-containing melts the position of the lance is of some importance since it is necessary not only to produce the iron vapour but also to provide some means whereby the iron vapour can be carried out of the metal bath and into the furnace atmosphere.

During desiliconization, that is, in the absence of a carbon boil where the carbon monoxide may act as carrier for the fume, it is feasible that if the end of the oxygen lance is submerged below the slag/metal interface then any vapour created will, in the absence of sufficient carrier gas, be re-dissolved to some extent by the overlying molten steel, the extent of re-solution depending on the depth of lance submersion and the presence or absence of excess gaseous oxygen to act as a carrier. If the lance is not adequately submerged it may be that the vapour is formed at a surface that is open to the furnace atmosphere, thus giving conditions suitable for greater fume formation. If such a postulate is correct the practical conclusion to be drawn is that one should be able to desiliconize steel and produce less fume by submerging the end of the oxygen lance well below the metal surface.

ACKNOWLEDGMENT

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Fumes from the acid open-hearth process

W. Strauss and M. W. Thring

SYNOPSIS

The concentrations and compositions of fumes from the acid OH furnace process have been determined at the various stages of the melting cycle. The concentrations are usually low, less than 0·4 g/nm³, but during oxygen lancing they rise to 6 g/nm³. Although the chief constituents of the fume are magnetite and hematite, other metallic oxides and silica form an appreciable part of the fume. 1669

INTRODUCTION

IN THE COURSE OF investigating methods of cleaning hot gases, small acid OH furnaces were used as a source of hot gases laden with fine particles. Although the waste gases from the basic OH process have been extensively investigated, no information has hitherto been published on the fumes from the acid process. Sampling of the gas before the experimental cleaner was necessary for the determination of cleaning efficiency, and at the same time gave a picture of the dust burden in the waste gases from the furnaces. Not only have the fume concentrations produced by such furnaces been measured but also the particle size distributions and, to some extent, the chemical composition of the fume.

The particular furnaces studied were producer-gas fired, and have a capacity of 22 tons each. The sequence of operations consisted of a charge of pig iron to insulate the banks, followed by two charges of scrap over a period of 3–4 h. A soft flame was then used to melt the charge over the next 3–4 h. Oxygen lancing was sometimes used to melt the last lumps, and to start the refining process or 'boil'. Iron ore was also frequently employed for the latter. After refining for several hours, till the required carbon concentration was reached in the melt, the boil was stopped by the addition of ferrosilicon alloy. Additives such as ferromanganese were introduced to bring the steel in the hearth up to the required specification, and the melt was then tapped. The furnaces were manually operated, and the lack of instrumentation may, in part, account for the wide range of fume concentrations and compositions found in the various stages of the process.

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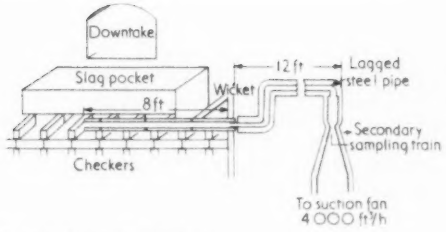
DETAILS OF THE INVESTIGATION

Sampling the fume

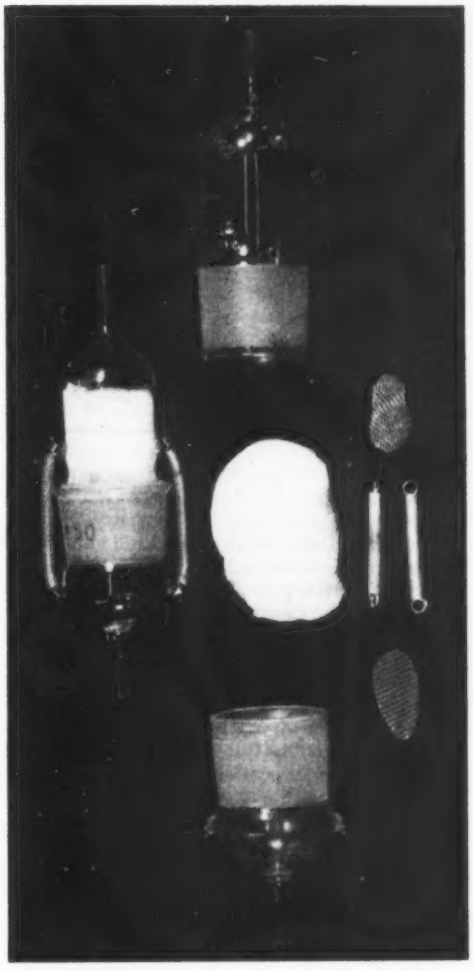
When sampling fume in a duct at high temperatures, it is usual practice to use a water-cooled probe facing upstream, and to sample the hot gases isokinetically, i.e. at the same velocity as they are passing along the duct. A major disadvantage of this method is that thermal precipitation occurs in the probe, and may



1 *Method of sampling hot gases*

cause the loss of an appreciable fraction of the fume in the sample. One way of overcoming this is to insert a ceramic liner into the water-cooled probe to keep the temperature of the gases above the dewpoint,[1] but the probe then becomes very cumbersome to use. An alternative solution is to insert a filter (in this case of sintered brass or stainless steel) in the tip of a water-cooled probe, and to have the probe entry lined with a removable stainless-steel liner. The amount deposited in the liner, as well as in the filter can be accurately found. A preliminary experiment, however, showed that the liner tended to block with the high fume concentrations, and it proved impossible to remove all the material deposited in the sintered-metal filter for quantitative analysis. Another disadvantage is that with the rapidly fluctuating process conditions and the high temperatures encountered, it is difficult to replace the filter and liner quickly enough to measure the fluctuations. Also the pore size of the filter (5 μ) is large compared with the fume particle size (usually less than 1 μ) and this type of filter depends largely on a sieving action, and not on impaction for its collection efficiency.

In the present experiments, therefore, a different technique was adopted, which attempted to avoid the disadvantages of the above methods. Essentially the method consists of sampling in two stages. In the first stage a ceramic tube, 2½ in. i.d., made up of sections 1 ft 6 in. long spigotted together (the joints being sealed with high-temperature cement), is projected 6–10 ft into the space above the checkerwork, and supported by the top row of checkers, about 3 ft from the wall of the slag pocket. The gas flow in the furnace was approximately at right-angles to the axis of the probe. The pipe was usually placed into the cold furnace, but repairs had often to be carried out to the system when it was hot. A 2-in. steel pipe, lagged with fibreglass



2 *Sampling filters; parts and assembled*

insulation, joined on to the ceramic pipe outside the furnace. This cleared the gas ducts, expanded to a 3-in. line, contracted to a 1½-in. throat, and then expanded again like a venturi diffuser. A large fan, with a

TABLE I Concentrations of fume from an acid OH furnace at various points in the steelmaking process (values in g/m³ at 0°C and 760 mm Hg pressure*)

	Charging							Refining and de-carburizing			
Furnace operation	Pig iron	Clean scrap	Oily scrap	Melting	Oxygen lancing	After lancing	Ore addition		Fe-Si addition	Tapping	Fettling
Mean fume concentration	0·75	0·18	0·32	0·24	5·97	0·48	1·74	1·76	0·64	0·47	0·17
Range of fume concentrations	0·21–1·91	0·12–0·49	0·12–0·52	0·09–1·01	3·43–8·90	0·14–1·00	...	0·21–4·14	0·54–0·73	0·22–0·83	0·10–0·23
Approximate values of mode(s)	0·4	0·2	0·3	0·15	6·0	0·2	...	†	0·6	0·3	0·15
No. of samples	4	3	2	18	8	4	1	23	2	4	2

* Actual concentrations at stack exit temperatures of 300°C are about one-half of these values.
† Concentrations are chiefly in the ranges 1·0–1·5 and 3·0–4·0 g/m³.

TABLE II Comparison of fume concentration in acid and basic OH furnace practice (concentrations in g/m³)

	Solid charging and melting	Hot-metal addition	Oxygen lancing	Refining	Fettling
Acid OH (present determinations usual values)	0·2–0·5	...	5·97	1·0–4·0	0·10–0·23
Basic OH					
Granville[3]					
Hot-metal furnace	0·55	0·53	0·92	0·58	0·16
Cold-metal furnace	0·39	...	...	0·42	0·09
Guthmann[4]					
Summary of various practices	0·12–1·0	to 4·6	2–13	0·9–4·6	...
Scrap high in lead and zinc	to 7·5	...	...	...	...
Richardson[5]	0·18–0·44	...	1·2–1·55	...	...

capacity of 4000 ft³/h, drew the primary sample through this system.

The secondary sampling line projected upstream into the centre of the diffuser (*see* Fig.1) and led to a filter, which was followed by a train consisting of a cooling coil, a catch-pot, a rotameter, and a suction pump. The secondary sampling line was of ¼ in. i.d., with an entrance velocity of about 30 ft/s, which was not very different from the velocity in the diffuser at the same point, so that here roughly isokinetic sampling was achieved. At the entry point of the gases into the furnace, where the temperature was about 1200°C, the gas velocity was about 60 ft/s. This may have eliminated some of the larger particles, but with an opening of 2½ in. dia. a certain proportion of these would certainly have been sampled. No particles larger than 6 μ (of which one was noticed) were seen on any of the large number of electron micrographs made. As it was reasonable to assume a continuous fume particle size distribution, larger particles (greater than 20 μ) would have been suspected if some particles approaching the thermal precipitator sampling limit of 20 μ had been present. In that case the method of sampling adopted here would have been unrealistic, but with the largely sub-micron sized particles encountered, this method of sampling could be relied upon for a representative sample.[2]

The temperatures of the gases sampled at the top of the regenerator were about 1100–1300°C, measured with a suction pyrometer. The temperature at the secondary sampling point was about 500–600°C. The high velocity and the high temperature in the lagged pipe should have reduced thermal precipitation to a minimum, and the venturi throat before the secondary sampling point should have avoided any tendency to stratification.

The secondary sampling line was of copper, was less than 2 ft long, and was not lagged. The filter itself was of a design suggested by Mr J. Pears of BISRA, and consisted of B50 sockets and cones, with ¼-in. glass tube entrance and exit lines (Fig.2) which are connected to the copper lines with short lengths of silicone rubber tubing. The filters were filled to depths of 4–5 cm with fine quartz wool supported on stainless-steel gauze. They proved to be virtually 100% efficient, even with the finest particles at high dust loads, with very small increases in pressure drop across the filter. The quartz wool was heat stable and inert to chemical attack.

Concentration of the fume

The weights of the fume collected in the filter were usually in the range of 50–500 mg. The filters were prepared by heating to 110°C, cooling and weighing, repeatedly, to constant weight (within 0·6 mg). The filters were similarly treated after fume collection. After passing the gases through the filter they were cooled in a copper coil immersed in water, the condensate eliminated in a catch-pot, and the flow rate measured in a rotameter. The corrections for the temperature and the pressure (less than 4 in. wg) were within the density calibration range for the rotameter. A possible source of error was that the temperature of the gases in the rotameter was greater than that of the water surrounding the cooling coils. No allowance has been made for this, and it could introduce an error of about ½%, which is less than the combined errors of filter weighing and sample volume metering in the determinations, which are estimated at less than 3%. The values of the fume concentrations are given at 0°C and 760 mm Hg, including the water vapour at the temperature of the cooling coils, and assuming the partly saturated air behaves as a perfect gas.

Chemical analysis of the fumes

The dust and fume retained on the quartz wool was ground up with the quartz wool to a fine powder, and fume from the stainless-steel gauze and from the walls of the filter was added. After 150 ml 2N hydrochloric acid and 5 ml 2 N nitric acid had been added to the powder, the mixture was heated and then filtered. The filtrate was made up to a known volume with distilled water. Parts of the solution were oxidized with 8 ml N nitric acid and 0·1 ml of 3% hydrogen peroxide, and then 5 ml of potassium thiocyanate solution were added and the solution made up to 50 ml. After standing for 20 min the optical density of the solution was measured on a Hilger absorption spectrophotometer (wavelength of 4750 Å). The iron in the solution, and so the total iron, was calculated by comparing the optical densities with those obtained for standardized iron solutions.

TABLE III Iron oxide (assumed Fe_2O_3) concentrations in OH furnace gases

Furnace operation	Fe_2O_3, % Charging Pig iron	Scrap	Melting	Oxygen lancing	Ore addition	Refining working	Fe–Si addition	Tapping	Fettling
Acid OH									
mean value	45	25	43	80	61	62	33	65	43
range	26–63	20–29	25–58	75–85	...	44–83	...	...	...
Basic OH[3]						Ore and lime boils working and decarburizing			
mean value	42	30				31		7	6
range	...	9–43				6–86		2–12	...

TABLE IV Analyses of fumes collected from various OH furnaces

Place of collection	Constituents, wt-% Fe_2O_3*	PbO	ZnO	CaO	SiO_2	Mn_3O_4†	MgO	Al_2O_3	P_2O_5	SO_3	Remarks	Reference
Dust collected by checkers	57·10	...	...	9·36	22·92	2·70	0·82	4·22	2·34	...	80-ton producer gas fired furnace, 7 months operation	11
Dust sucked from slag pocket	33·45	...	10·45	25·5	6·02	1·40	6·62	0·58	...	7·10	coke-oven gas + brown coal addition	12
Deposition in slag pocket	73·7	...	...	4·54	2·03	4·37	7·70	5·84	0·19	...	oil-fired acid furnace (30 tons)	13
Dust at base of checkers	49·7	...	16·80	2·88	2·79	6·74	1·12	3·29	0·13	12·50	as in ref. 13	13
Dust at top of checkers	36·36	0·60	3·25	4·15	27·52	1·46	1·31	6·65	0·43	10·02	30-ton oil-fired basic furnace and high lead and zinc scrap	14
Dust at base of checkers	13·54	6·35	32·75	4·76	5·75	1·16	0·162	2·95	0·67	20·86	as for ref. 13	13
Dust deposited in waste-heat boiler tubes	26·35	...	24·38	3·41	1·01	5·06	1·95	6·56	0·39	16·38	(red-brown friable incrustation)	13
	11·76	7·29	38·36	3·82	0·42	0·55	0·34	0·48	0·72	26·61	as for ref. 14	14
Dust deposited in outlet duct	88·3	...	...	6·48	3·77	3·14	1·70	1·35	0·23	0·10	as for ref. 12	12
Dust collected with venturi scrubber	87·5	...	...	0·3–1·4	1·1–2·6	0·4–1·1	traces	0·2–1·9	...	2·7–4·1	oxygen lancing	15
Dust deposited in chimney	35·38	...	21·21	2·73	1·46	7·90	0·57	3·10	0·59	19·79	as for ref. 13 (red-brown friable deposit)	13

* Fe_2O_3 calculated from total Fe. † Mn_3O_4 calculated from total Mn.

RESULTS AND DISCUSSION

Fume concentrations

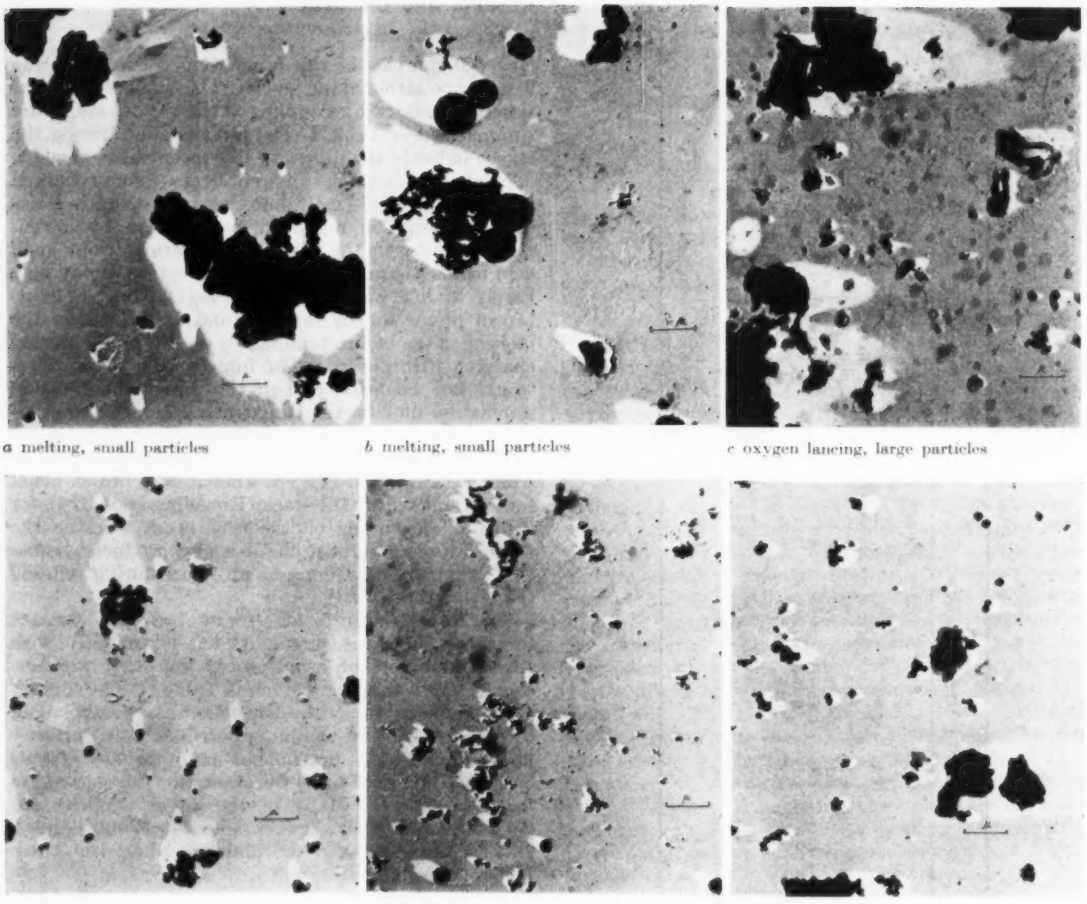
Over 70 samples of the fume were collected during 9 separate steelmaking cycles. The fume concentrations determined by sampling at the various stages are summarized in Table I. The ranges found during the stages indicate the diversity of concentrations, which depend not only on the different types of scrap charged and the state of the melt, but also upon operating conditions controlled manually rather than by instruments. However, the fume concentrations from small acid furnaces are, in general, not great; and may be a barely visible white plume during fettling; a light-brown plume during tapping, and at the end of the melting stage before refining; or a grey plume during charging with pig iron. The fume during these stages would not be heavier than the optical obscuration implied by no.2 on the Ringelman scale, the approximate values of which are given to indicate the density of the smoke. However, heavy fuming does occur while working with oxygen, when a deep brown-black fume is emitted by the stack (Ringelman nos. 3, 4), for 2–8 min, depending on the time spent working the melt. An orange-brown plume is also produced, sometimes for periods as long as an hour, during refining (Ringelman nos. 2, 3).

The fume concentrations for the acid OH furnace found in these measurements agree generally with those found for the basic process by other workers. Comparative values are given in Table II. The slightly lower values obtained by Granville[3] can be explained by the fact that his sampling positions were half-way up a chimney stack (for the hot-metal furnace, case *A*) or at the duct leading to the stack (for the cold-metal furnace, case *B*). Many of the larger particles are deposited in the checkerwork, waste-heat boiler tubes, and ducting, and also the plume is diluted at these points with infiltrating air.

Fume composition

Most of the fume samples were analysed for the total amount of iron present, as is described above. The iron concentrations have been expressed as Fe_2O_3, as other workers have done, and are listed in Table III. It has been stated[6,7] that the fume contains about 90% of iron oxide but present analyses show that the iron oxide content is much less for most of the steelmelting cycle. The present values can be compared to those obtained by Billings, Small, and Silverman[8] for the iron oxide content of the fume drawn from an OH furnace when investigating their slag-wool filter (Table III). The iron oxide content of the fume deposited in the checkerwork and in the waste-heat boiler tubes is also of the same order as found for the present determinations (Table IV).

X-ray diffraction patterns of the fume during melting, oxygen lancing, and refining showed that the iron in the fume is in the form of a mixture of hematite (Fe_2O_3) and magnetite(Fe_3O_4) during melting and refining, and is almost wholly magnetite during oxygen lancing. The error in assuming the oxide to be pure hematite, and not a mixture of the two oxides is usually less than 1% for the total amount of oxide present. Changing the iron oxide percentages in Table III is not justified with the ranges of values found in the present work.



a melting, small particles *b* melting, small particles *c* oxygen lancing, large particles

d oxygen lancing, small particles *e* refining, large particles *f* refining, small particles

3 *Electron micrographs illustrating the types of particle occurring in the fume (Crown Copyright SMRE)* ×5400 approx.

The other constituents of the fume depend very much on the type of scrap charged. A sample of fume taken during melting was examined spectrographically and showed the oxides of manganese, nickel, copper, and silica to be present in appreciable quantities, together with smaller quantities of a number of others. During refining a number of these, initially present in appreciable quantities, had been reduced to concentrations of less than 1%. For comparison, the more extensive chemical analyses of the fume collected by sampling the gas and of that deposited in checkers and waste-heat boiler tubes, made by other workers is given in Table IV.

Fume particle size

The fume was sampled with a thermal precipitator from a steel tube with conical ends (4 in. dia. and 1 ft long) placed in the secondary sampling line instead of the glass filter. Heat-stable membranes were used in the thermal precipitator, and the tube was purged with nitrogen before a sample was taken. Fume was sampled during charging, while oxygen lancing, and during refining, and the membrane was subsequently examined under an electron microscope. There appeared to be no significant difference in the particles between the stages. Typical micrographs, which illustrate both the large and small particles are shown in Fig.3. The particles consist mainly of two types, the first is about 0·8–1 μ dia., and occurs singly or in agglomerates up to about 4–6 μ; the other type is much smaller, usually less than 0·1 μ and tends to form chains, which are less than 1 μ long. The heat-stable membranes were heated up to 1000°C, but no change in the particles could be seen, indicating that they are metallic oxides or silica. It was not possible to determine any chemical difference between the larger particles and the chains, although chain formation indicates that the particles are magnetic. Similar chains and some of the crystal-like agglomerates have been found by Meldau *et al.*[9] in the fumes from arc furnaces and from oxygen-blown converters. The larger round particles are similar to those sampled by Silverman.[10]

These measurements of fume concentrations from an acid OH furnace indicate that heavy fuming occurs only for a few minutes when oxygen lancing is used to speed the melting process. Fuming to a lesser extent, but for longer periods, occurs when refining. It is only during these periods that this type of furnace constitutes a serious source of air pollution.

ACKNOWLEDGMENTS

The present investigation forms part of the studies in high-temperature gas cleaning being carried out in the Department of Fuel Technology and Chemical Engineering at the University of Sheffield, and sponsored by the Ministry of Power. The authors are indebted to members of the pneumoconiosis group of SMRE for the electron micrographs, the X-ray diffraction patterns, and the facilities for carrying out the chemical analysis. In the last they were assisted by Miss J. Gelsthorp. The authors would also like to thank Mr D. Bradbury and the Darwins Group of Companies for permitting the sampling at their Andrews Toledo Steel Works.

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An electron diffraction method for determining the crystal lattices of small thin lamellar precipitates occurring in steels

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SYNOPSIS

A method has been developed for determining the crystal lattices of individual plate-like precipitates occurring in steels. The precipitates are isolated in an extraction replica and electron diffraction patterns recorded from precipitates with the extraction replica inclined at a number of angles from 90° to 10° to the electron beam. Tilting of the replicas is achieved by using a new stage of simple design or by placing the replicas in small fixed inserts that fit into a standard specimen holder. The various types of two-dimensional spot electron diffraction pattern obtained correspond to different zone axes, and enable the full crystal lattice of the precipitates to be deduced. The technique is particularly valuable for determining crystal lattices of small thin particles for which X-ray diffraction methods fail. Application of the method to plate-like cementite (Fe_3C) *and iron nitride* (α''-$Fe_{16}N_2$) *precipitates is described.* 1682

INTRODUCTION

PLATE-LIKE PRECIPITATES occurring in steels are frequently only a few microns in lateral extent and the order of 0·01μ in thickness. Consequently, methods of examination suitable for determining the identity and crystallographic habit planes of such precipitates are restricted, in general, to electron microscopy and electron diffraction. The direct examination of precipitates in a steel matrix by reflection electron diffraction[1] is not entirely satisfactory, and bulk extraction methods for the isolation of precipitates from a matrix suffer the disadvantage that the extremely thin, fragile plates may break up or dissolve before being collected and examined. More satisfactory results are obtained when extraction replicas[2,3] are used to isolate the precipitates, because only mild chemical treatments are employed and the replica film provides mechanical support for the extracted precipitates.

Examination of numerous plate-like precipitates in extraction replicas by the selected area transmission electron diffraction technique available with three-stage electron microscopes has shown that they mostly occur as single crystals giving regular spot patterns of the 'two-dimensional' type. However, since the more completely extracted plate-like precipitates present in such replicas generally lie with the plane of the plate roughly parallel to the plane of the supporting film, and hence normal to the electron beam, the complete crystal lattice cannot usually be deduced from the electron diffraction

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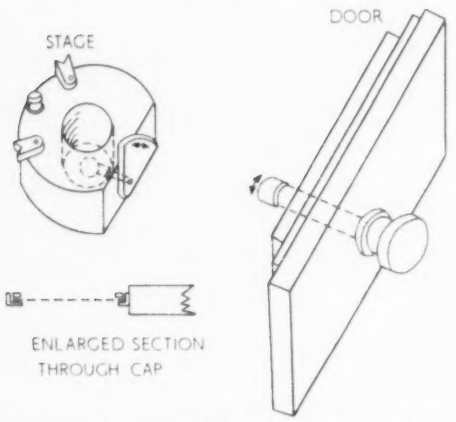


1 *New stage and door suitable for tilting specimen grids through wide angles in a Metropolitan-Vickers type EM3 electron microscope*

patterns. The relative positions and spacings of the atoms lying in the plane of the plates may be calculated but not those in a direction perpendicular to the plates.

The same difficulty has been encountered by other workers. Gard[4] found that small, plate-like crystals of certain mineral substances placed on substrate films, usually lay with the plane of the plates closely parallel to the films, and that each type gave a single 'two-dimensional' spot electron diffraction pattern. As these crystals varied in thickness from about $0{\cdot}02\mu$ to 1μ[5] the third diffraction condition generally had some influence on the intensities of the spots of the pattern, giving rise to well defined high and low intensity Laue zones. When the substrate films, and hence the crystals, were tilted by the stereoscopic tilting mechanism of a Metropolitan-Vickers type EM3 electron microscope through small angles (up to about 7°) from a position perpendicular to the electron beam, the positions of the spots in the patterns remained unchanged, but the location of the high and low intensity zones was altered. From measurements on the positions of the zones and the angles of tilt Gard[4] was able to estimate the spacing of the atoms in the direction perpendicular to the plates.

Many plate-like precipitates occurring in steels are too thin for Gard's method[4] to be applied, and so an alternative method has been devised which is simpler in application, more accurate, and can be applied to plate-like particles with thicknesses up to several hundred Å. It involves isolating the precipitates in an extraction replica and recording electron diffraction patterns from precipitates with the extraction replica inclined at a number of angles from 90° to 10° to the electron beam. In this manner various types of pattern are obtained corresponding to different zone axes, and from these patterns the complete crystal lattice of the precipitates can be derived without accurately knowing the angles of tilt employed. Tilting has been achieved by employing a new continuously tilting electron microscope stage of simple design, or by placing the extraction replicas in small fixed inserts that fit into a standard electron microscope specimen holder. Two examples are given to illustrate the method.

APPARATUS FOR TILTING THE EXTRACTION REPLICAS

Although the details of the continuously tilting stage and fixed inserts used to tilt the replicas have been given elsewhere,[6] a brief description is included here so that the method of examination may be more clearly understood.

Continuously tilting stage

The continuously tilting stage (Fig.1) devised for the Metropolitan-Vickers type EM3 electron microscope consists of a solid brass block with a large central hole and carries the normal stage lateral movement controls. A horizontal shaft passes through one half of the base of the block and has attached to it a shallow brass cap at the inner end and a vertical brass sector at the outer end. The cap holds the extraction replica and is so arranged that movement of the sector from the position with one edge horizontal (as shown in Fig.1) to the position where the other edge is horizontal rotates the plane of the replica from the horizontal to a near-vertical position. This movement is achieved during operation of the microscope via a friction-drive shaft acting through a specially constructed specimen chamber door. The shaft passes through a Wilson vacuum seal built into the door, thereby preventing leakage of air into the microscope when the shaft is rotated. A neoprene sleeve fitted to the end of the shaft in contact with the sector ensures a smooth slip-free movement. It is, therefore, a simple matter to calibrate the rotation of the shaft in terms of the angle of tilt of the replica.

Fixed inserts

Each of the fixed inserts (Fig.2) consists of two parts which, when placed together and fitted into a standard electron microscope specimen holder, sandwich an extraction replica at a predetermined angle to the longitudinal axis of the holder, and hence to the electron beam. The 45° insert of Fig.2*a* fits into a standard $\frac{1}{8}$-in. dia. specimen cap, but the 10° insert of Fig.2*b* is held together by a spring clip. The latter insert is shown in solid form rather than section as it illustrates its construction more clearly. Similar inserts can be made to provide other angles of inclination, but the two described here have proved adequate for most lattice determinations.

METHOD OF EXAMINATION

In the first method the extraction replica is placed in the new stage, set perpendicular to the electron beam and an electron diffraction pattern recorded from a suitable precipitate. The replica is then tilted through various angles until it is almost parallel to the beam and further diffraction patterns from the precipitate recorded, several different types of pattern thereby being obtained from an individual precipitate. Alternatively, the replica can be set at a particular angle to the electron beam, patterns recorded from suitable precipitates and the procedure then repeated with other angles of tilt. The examination is continued until a sufficient number of patterns have been obtained.

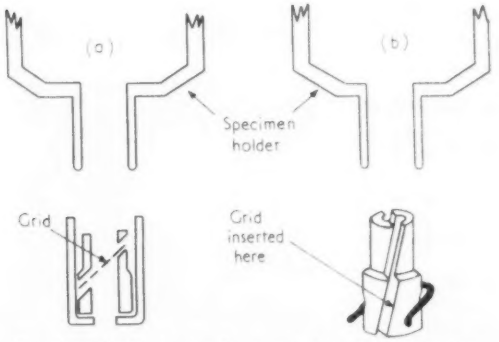
In the second method, the extraction replica is set successively (*a*) perpendicular, (*b*) at a preset angle of 45°, and (*c*) at a preset angle of 10° to the electron beam and electron diffraction patterns recorded from

suitable precipitates in each instance. The required angles of inclination are obtained by placing the replica in the standard specimen holder in the conventional manner and then in each of the two specially constructed inserts that fit into the holder. The small variation in the angle between the plane of the plate-like precipitates and the plane of the extraction replica, together with the random orientation of the plates about an axis perpendicular to the replica, provides sufficient latitude for a reasonable number of precipitates to be suitably oriented to give good electron diffraction patterns without the necessity of altering further the angle of tilt of the grid.

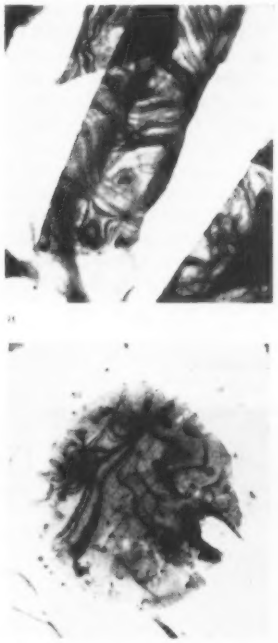
Another way in which electron diffraction data can be usefully recorded from plate-like precipitates present in an extraction replica is to tilt the replica at, say, 45° to the electron beam and slowly scan it during the exposure so that a number of favourably oriented precipitates contribute to the pattern, which consists of numerous spotty arcs and rings. Although the individuality of single precipitates is lost by scanning, many reflections that do not usually occur in patterns from replicas held perpendicular to the electron beam are recorded.

When using the new stage or the inserts, the grids are at a greater distance from the objective lens than in normal practice, and precipitates at different positions on tilted grids are usually at different distances from the lens. The diffraction calibration constant of the microscope is therefore altered and may vary from exposure to exposure. These differences can be taken into account and an accurate calibration provided for each pattern, by evaporating a thin gold film on to some areas of the replica and recording electron diffraction patterns in pairs, one from a suitably oriented precipitate in an area free from gold, and the other from a neighbouring region containing gold. Although both types of pattern can be recorded simultaneously from suitable areas, the above method is preferred, since it avoids the loss of detail in the precipitate pattern which usually results from simultaneous recording.

When the usual type of electron microscope grid is tilted so that the plane of the grid makes angles of about 10–20° with the direction of the electron beam, the considerable fore-shortening that takes place along the direction of maximum slope of the grid results in a large number of grid bars crossing the electron image in one direction, and may present some difficulties.



2 *Specimen holder inserts for examining grids inclined at (a) 45°, and (b) 10° to the electron beam*



3 *Electron micrographs of plate-like precipitates of (a) iron carbide* (Fe_3C)*, and (b) iron nitride* ($Fe_{16}N_2$) *present in extraction replicas* ×8000

This can be largely overcome by using grids containing long parallel rectangular slots extending almost completely across the grid (Sjöstrand type) and aligning the grids so that when they are tilted, the slots lie along the direction of maximum slope.

APPLICATION

Iron carbide (Fe_3C)

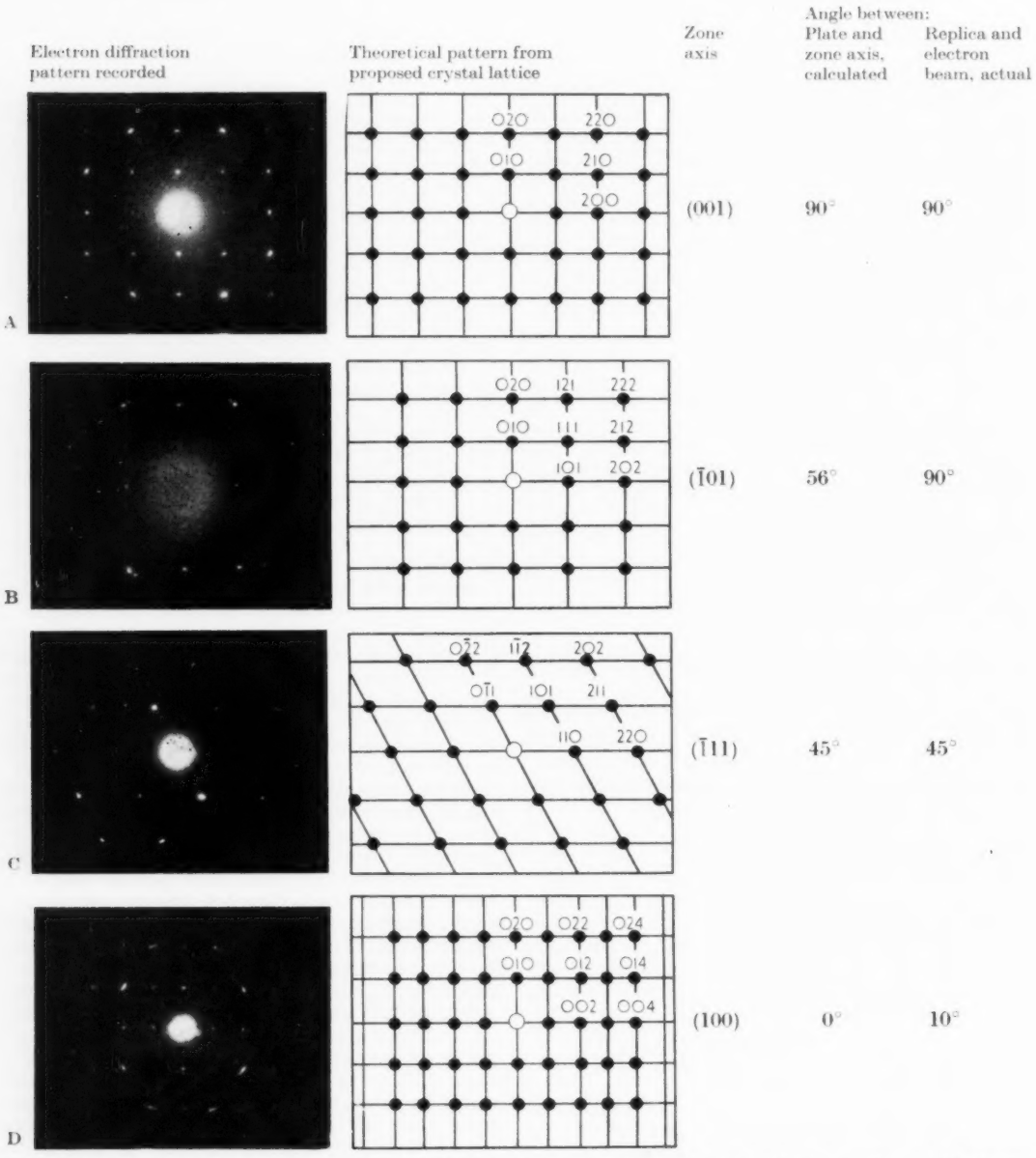
Extraction replicas from a coarse pearlite structure in a plain carbon steel contained a number of thin (order of 0·01μ) cementite plates (Fig.3*a*) corresponding to areas of regular pearlite lamellae. Using the fixed insert method for tilting the replicas, several transmission electron diffraction patterns were recorded from plate-like precipitates. Four of these are illustrated in Fig.4, column 1.

The pattern of row *A* was obtained with the beam roughly perpendicular to one of the plates. It shows that two major crystallographic axes lie in the plane of the plate, are perpendicular to one another, and contain atoms 4·51 Å and 5·08 Å apart. The pattern of row *D* was recorded with the grid tilted at 10° to the beam and indicates two perpendicular axes with spacings of 5·08 Å and 6·72 Å. The two patterns taken together suggest that the precipitates possess an orthorhombic structure with a=4·51 Å, b=5·08 Å, and c=6·72 Å and grow with the 'c' axis perpendicular to the plane of the plates. These patterns may be compared with those illustrated in rows *A* and *D*, column 2, which are the patterns that theoretically would be obtained in this manner from such a structure. Each pattern may be considered as an intersection of the Ewald sphere (which in electron diffraction studies is virtually a plane) with the reciprocal lattice of the crystal structure.

The reciprocal lattice of the proposed structure indicated that the row *B* pattern might correspond to a ($\bar{1}01$) zone axis. Subsequent calculation showed that the pattern expected on this assumption (row *B*, column 2) agreed with that actually obtained. The row *C* pattern was found to be a reasonable fit with that corresponding to a ($\bar{1}11$) zone axis (row *C*, column 2). This additional evidence afforded confirmation of the correctness of the proposed crystal lattice. The unit cell dimensions for cementite deduced by this method are in agreement with those obtained by Hume-Rothery, Raynor, and Little[7] by X-ray diffraction methods, namely; orthorhombic with $a=4{\cdot}5155$ Å, $b=5{\cdot}0773$ Å, and $c=6{\cdot}7265$ Å.

Iron nitride ($Fe_{16}N_2$)

Iron nitride (α''-$Fe_{16}N_2$) precipitates formed in pure iron by nitriding and furnace cooling were isolated in extraction replicas and found to be thin (order of $0{\cdot}01\mu$) plates (Fig.3*b*). Four electron diffraction patterns from these precipitates obtained in a similar manner to those of the cementite precipitates are illustrated in Fig.5, column 1. The patterns of row *A* and row *D* taken together indicate that the precipi-

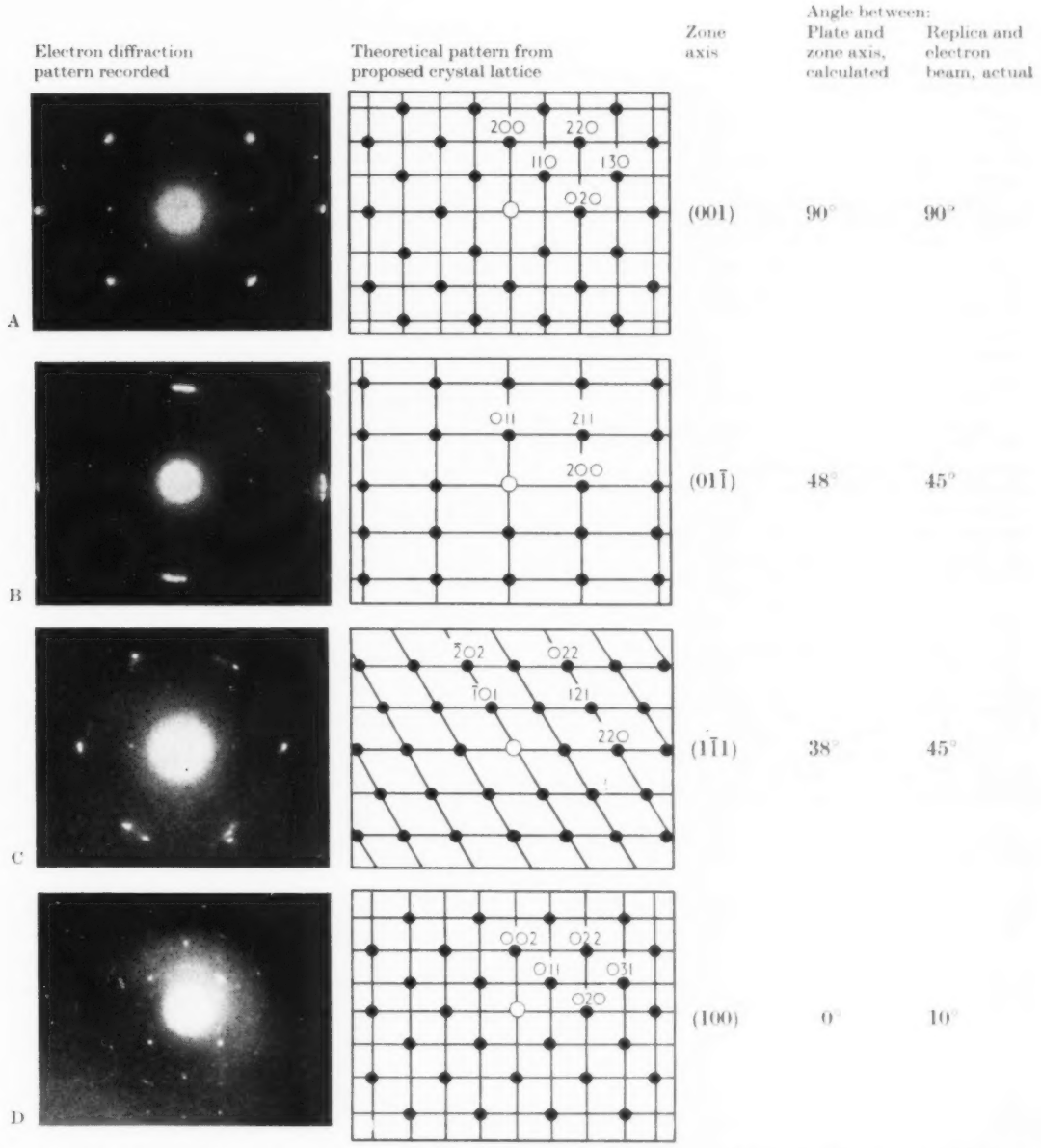


4 *Analysis of electron diffraction patterns obtained from plate-like cementite precipitates constituting regular pearlite structure with the precipitates at various angles to the electron beam*

tates possess a tetragonal structure with $a=5{\cdot}72$ Å and $c=6{\cdot}30$ Å and that the 'c' axis is perpendicular to the plane of the plates. The patterns of rows B and C correspond to $(01\bar{1})$ and $(1\bar{1}1)$ zone axes respectively. The unit cell deduced for these precipitates is in good agreement with that found by Jack[8] by X-ray diffraction methods, namely, tetragonal with $a=5{\cdot}711$ Å and $c=6{\cdot}291$ Å. The absence of reflections in the patterns of rows A and B with indices such that $(h+k+l)$ is odd is characteristic of a body-centred tetragonal lattice. The patterns of column 2 are those that would theoretically be obtained from such a structure. Although this is supported by the stronger reflections of pattern D, some weak 'forbidden' reflections are also present, suggesting, perhaps, a more complicated structure. Jack[8] reported that the α''-$Fe_{16}N_2$ unit cell consists of 8 distorted and expanded body-centred tetragonal units.

DISCUSSION

The main advantage of this method of examination is that the unit cell dimensions can be determined by electron diffraction studies on small, thin, plate-like precipitates occurring in metals and which, because of



5 *Analysis of electron diffraction patterns obtained from plate-like precipitates of* α''-$Fe_{16}N_2$ *iron nitride with the precipitates at various angles to the electron beam*

their extreme thinness, cannot normally be studied by X-ray diffraction methods. Since many of the precipitates formed in metals are of this form, the method is of wide application.

The ability to tilt extraction replicas containing such precipitates through wide angles in a three-stage electron microscope enables a variety of electron diffraction patterns to be obtained from which the type, size, and orientation of the reciprocal lattices of the precipitates, and consequently their crystal lattices, can be deduced. The new stage enables either individual or random plate-like precipitates to be examined as they are progressively tilted from a position with the plane of the plates roughly perpendicular to the electron beam to a roughly parallel position. The fixed insert method, although allowing less latitude in the orientation of the precipitates and generally restricting the examination to random rather than individual precipitates, is simpler and quicker to use. The latter method is particularly useful for those electron microscopes not amenable to the fitting of a continuously tilting stage.

The electron diffraction patterns of Figs.4 and 5 clearly demonstrate the orthorhombic and tetragonal crystal lattices of the Fe_3C and $Fe_{16}N_2$ precipitates, respectively, and show that both types of precipitate grow with the '*c*' crystallographic axis perpendicular to the plane of the plates. Although this is the habit plane predicted by Jack[8] for $Fe_{16}N_2$ precipitates theoretical considerations have not yet enabled a simple habit plane to be deduced for the growth of cementite plates in a γ-iron matrix.[9]

For each of the illustrated patterns of Figs.4 and 5, the calculated angle between the plate and zone axis (column 4) is, in general, close to the angle between the replica and electron beam (column 5). As the former angles were calculated from the crystallographic information deduced from this examination, the good agreement indicates consistent results. The one exception is the Fe_3C row *B* pattern, the replica being at 90° to the beam and the zone axis at about 56° to the plate. This difference is thought to be due to the cementite plates in this instance being oriented at a large angle (30–40°) to the plane of the replica film. Experience has shown that this effect does sometimes occur with plate-like precipitates large in lateral extent, but infrequently with small precipitates.

The two examples given illustrate further the extent to which extraction replicas allow individual precipitates to be examined separately, even when the precipitates are present as minor included phases. Thus, the pearlite specimen contained considerably more solid Fe_3C particles than thin plates, whilst the nitrided iron specimen contained, in addition to α''-$Fe_{16}N_2$ precipitates, γ'-Fe_4N precipitates and grain-boundary included material. The presence of two or three closely grouped spots in some of the patterns (e.g. Fig.5, row *C*) in the position of the one spot that would be expected from a single crystal is thought to be because these particular patterns were recorded with a relatively large limiting electron diffraction aperture, and so more than one precipitate of the same type, similarly but not exactly aligned with respect to one another, contributed to the pattern.

It is interesting to find that the unit cell dimensions determined by this method for cementite (Fe_3C) and iron nitride ($Fe_{16}N_2$) are in such close agreement with those determined by X-ray methods. The Fe_3C particles examined by Hume-Rothery, Raynor, and Little[7] consisted chiefly of solid particles rather than thin plates, whilst the $Fe_{16}N_2$ precipitates studied by Jack[8] were produced by nitriding pure iron powders. When small, thin precipitates are grown in a solid matrix, some distortion in the size of the lattice might be expected, but the information provided by the tilting method establishes that the unit cell dimensions of these two precipitates are in close agreement with those of the bulk materials. Furthermore, the absence of streaks in the diffraction patterns indicates that these precipitates are free from major growth faults.

ACKNOWLEDGMENT

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Heat reflectors in forging

A. R. Hardy, A.I.M., and J. D. Stringer, B.Sc.(Tech)

SYNOPSIS

This paper describes an investigation into the use of heat reflectors as a way of retarding the cooling of hot steel whilst it is being forged, with consequent gains in the time available for forging before reheating is needed. The forging is surrounded by reflectors which intercept radiated heat and reflect it back on to the hot surface. The investigation consisted of measuring the effects obtained when hot bars were held on the axis of sheet-metal tubes and of comparing forging rates using a reflector assembly designed on the basis of the information obtained. It was found that a bar inside a tube can cool up to four times more slowly than in air and whilst the effects were less marked when using reflectors to shield hot steel during actual forging operations, an increase of about 50% in the available forging time (without reheating) has been obtained when cogging square bars. 1775

INTRODUCTION

IN FORGING PROCESSES, heating costs have risen in recent years because of rising fuel costs, and methods of improving heating efficiencies are being sought by way of improved furnace design and faster heating.[1,2] A less orthodox approach to fuel saving is to retard the cooling of the hot metal during the actual forging operation, thereby reducing the amount of heat needed.

The primary cause of heat loss from a forging above 800°C is due to radiation and the possibility of reducing this loss by the use of reflectors was considered sufficiently promising to warrant further investigation. Heat transfer theory indicated the criteria likely to govern the efficiency of such reflectors and experiments were made to assess the practical possibilities.

Much of the work consisted of comparing the heat losses from hot bars cooling freely in air with the losses when cooling inside tubes made from different types of metal sheeting.

From the data obtained, a system of reflectors was designed for use on the BISRA experimental forge and comparative trials were made with and without the reflectors in use.

BAR COOLING EXPERIMENTS

To investigate the factors affecting the efficiency of reflective shields, two types of experiment were carried out. The first consisted of heating steel bars to forging temperature (about 1250°C) and then comparing the cooling rates in air with those when the bars were placed inside various tubes. In the second series an electric element simulated the hot bar.

Heat transfer theory indicated that the main factors likely to affect the efficiency of the reflectors were:

(i) reflectivity of the tube surface
(ii) tube size
(iii) closure of the tube ends.

The reflectivity of a surface is defined as the ratio of the reflected to the total radiant energy falling upon the surface. The reflectivity varies with material, surface smoothness, and is affected by the presence of oxide films.

The size of each tube has been expressed as the ratio of tube diameter and bar diameter and will be termed the T/β ratio. Larger tubes were expected to have a lower shielding efficiency than smaller tubes because of the greater surface area available for absorbing radiation. Closure of the tube ends, it was thought, might improve the shielding efficiency by reducing the convective heat loss from the hot bars.

Procedure

Continuous cooling tests All test bars were of low-carbon steel quality and measured 4 ft 6 in. in length and 3 in., 4 in., or 6 in. in diameter. Two $\frac{1}{4}$-in. holes were drilled diametrically opposite in each bar, at roughly mid-length, extending respectively to the centre of the bar and to within $\frac{1}{8}$ in. of the opposite surface, to accommodate Pt/Pt–Rh thermocouples. The bars were heated to 1250°C in a gas-fired furnace and held for 30 min to ensure temperature equalization throughout. They were then removed from the furnace, held horizontally, and allowed to cool, either in air or situated co-axially within a reflector tube. Three tubes were used in the experiments namely: an 11-in. dia. stainless-steel tube, a 15-in. dia. stainless-steel tube, and a 12-in. dia. aluminium tube. Each tube was 4 ft long and fabricated from sheet metal about 0·030 in. thick.

The surface and centre temperatures of the bars were continuously recorded during cooling and a measure of the tube temperature was also obtained from thermocouples clipped to the top of each tube.

Tests with electric element A 'Kanthal'-wound heating element, 9 in. long and 0·56 in. dia. was used to

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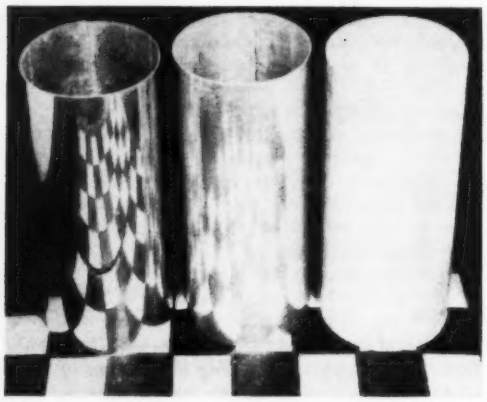
The authors were at the Association's Sheffield laboratories. Mr Hardy is now with the UKAEA (Industrial Division) and Mr Stringer is with the Mechanical Engineering Department, University of Sheffield.



1 *Aluminium reflector tubes*

simulate a hot steel bar. It was heated electrically and its temperature measured by means of a Pt/Pt–Rh thermocouple inside the hollow element. The element was successively heated to various temperatures within the range 700–1 200°C and the electrical powers required to maintain steady temperatures were compared when the bar was supported horizontally in air, and when in one of a number of reflector tubes.

The tubes for these experiments were all fabricated from 0·030-in. aluminium sheet and were 9 in. long, but the diameters and the surface finish of the tubes were varied. Four diameters namely 1 in., 2 in., 4 in., and 6 in., and three surface finishes were employed. The relative appearances of the surface finishes are shown in Fig.1; the best finish showed fewer rolling marks than the second best, whilst the third was matt. In some of the tests circular asbestos plates were fitted to the tube ends.

Results

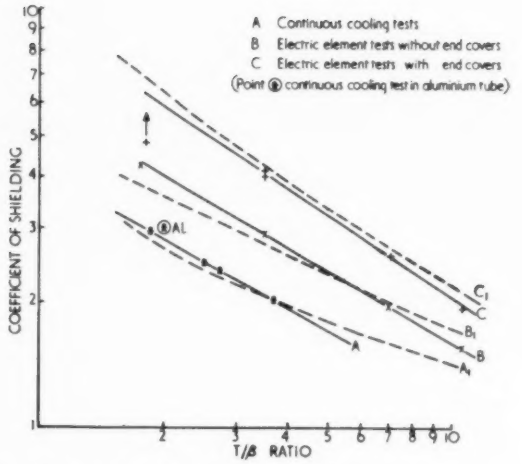
The effectiveness of each tube in reducing the heat lost from a bar has been expressed as the coefficient of shielding. In the case of the continuous cooling tests this is the ratio of the times taken for bar to cool from 1 200° to 800°C in the tube and in free air, respectively (reciprocal of the cooling time is a measure of the average rate of heat loss from the bar). For the electric element tests, it is the ratio of the watts input to maintain a given temperature in air to that in a tube, (the electrical power input being a measure of the rate of heat loss from the electric element). The latter ratio varied slightly with the bar temperature and the results are given as an average ratio for tests in the range 700–1 200°C.

The tests were analysed to find the effect on the coefficient of shielding of the three factors which were varied in the experiments and the results are as follows.

Effect of tube reflectivity

The results of tests summarized in Table I show that large changes in reflectivity produce correspondingly large changes in the shielding coefficient.

The first test was made on a new highly polished stainless-steel tube. The second was made at a later date on the same tube after it had been used in other tests in which the surface oxidized progressively, presumably because the tube had reached 300°C in



2 *Effect of tube size on coefficient shielding*

most tests and in one of them had approached 700°C. The material used for the third test had a rough and oxidized surface. The experimental results are in reasonable agreement with shielding coefficients calculated from simplified heat transfer equations (*see* Appendix).

Small changes in reflectivity, as represented by the three types of surface finish on the aluminium sheets shown in Fig.1 had no marked effect on the shielding coefficient. The coefficients of the matt surface were lower, but never more than 5% below those obtained with the two rolled finishes.

The reflectivity of aluminium did not appear to diminish with use as was the case with stainless steel. Five electric element tests, each lasting 6 h, were made with the matt 4-in. dia. tube, and although the tube maintained its maximum temperature of 500°C for at least ½ h in each test, consistent results were obtained throughout.

Effect of tube size

The effect of the tube size on the coefficient of shielding can be seen from curves A and B in Fig.2; A is for continuous cooling tests in stainless-steel tubes and B for electric element tests in aluminium tubes. In both types of test, the larger the T/β ratio, the lower the coefficients of shielding. The relative position of the curves suggests that stainless steel has a lower reflectivity than aluminium.

The associated curves A_1 and B_1 represent theoretical calculations and are in good agreement with the experimental curves. The theoretical curves were

TABLE I Effect of tube reflectivity

Continuous cooling experiments T/β ratio = 1·85/1		Theoretical calculation T/β ratio = 1·75/1; assumed bar emissivity = 0·8	
Tube material	Shielding coefficient	Tube reflectivity	Shielding coefficient
Stainless steel (new)	3·0	0·9	3·9
		0·8	2·9
Stainless steel (oxidized)	2·0	0·7	2·3
Hot-rolled mild-steel sheet	1·6	0·6	1·7

TABLE II Size of (open-ended) tube and temperature of bar which together caused tube temperatures of about 600° and 300°C

Relative tube size T/β ratio	Bar temperature, °C 600°C tube	300°C tube
2	1100	750°C
3	1250	900°C
4	1350–1400°C*	1000°C
5	...	1100°C
10	...	1350°C*

* extrapolated values
Note: closed tubes were up to 100°C hotter

obtained by assuming a bar emissivity of 0·8 and by assuming natural convection cooling of the bar to take place inside the tube. Curve A_1 is based on a tube reflectivity of 0·2 and curve B_1 of 0·1.

The effect of closure of the tube ends

Curve C of Fig.2 was obtained from electric element tests with asbestos plates on the ends of the tubes. The effect is to increase the coefficient of shielding for any given T/β ratio, but the general trend of larger tubes being less effective is retained. The curve C_1 is the result of theoretical calculations similar to those for curve B_1, but with the assumption that no convection occurs in the tube. Thus, closing the ends of the tube appears to minimize convective heat losses from the hot bar.

Discussion of results

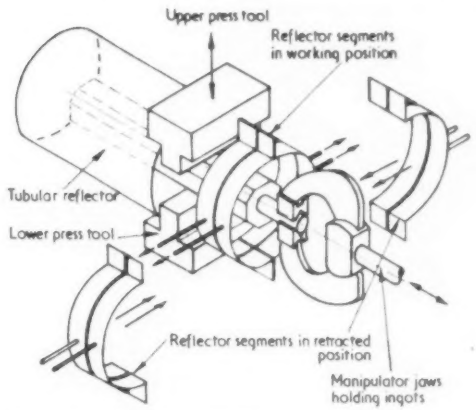
Shielding appears to be most effective when small-diameter tubes of relatively shiny material are used. The smallest size that can be used is governed by the temperature of the tube; an unduly high temperature accelerates surface oxidation whereby reflectivity is impaired, or, in the case of aluminium, causes the tube to melt.

Aluminium was found to be a better reflector than stainless steel; temperatures up to 600°C can be tolerated and the high reflectivity appears to be retained up to this temperature. The aluminium oxide layer formed is presumably thin and does not absorb much heat. The reflectivity of stainless steel appears to deteriorate above about 300°C, presumably because of the formation of a heat-absorptive oxide layer. The degree of surface finish on commercially available aluminium sheeting is relatively unimportant.

The temperature reached by the tube in each test could be roughly correlated with the bar temperature and the size of the tube (expressed in terms of the T/β ratio). However, considerable scatter was evident probably due to uncontrolled air currents and the simplified heat transfer theory was not found suitable for the prediction of tube temperatures. Values deduced from the results which were obtained are given in Table II which may be used as a rough guide in designing reflector installations.

FORGING TRIALS WITH REFLECTIVE SHROUDS

To establish the potential value of heat reflectors as a means of prolonging forging operations, reflective shields were designed for use on the BISRA experimental forge and steel bars were cogged with and without the shields in use. The arrangement of shields is shown diagrammatically in Fig.3; it comprised a



3 *Diagram of shield arrangement*

tube on the free side of the press, semi-circular pieces fixed to each press platen (not shown), and a series of retractable semi-circular pieces on the side of the press near the manipulator. The shields were made of aluminium because it retains its reflectivity and is relatively cheap.

Ingots, 8-in. square, were to be used for the trials and the surface temperature was expected to be 1050°C or 1100°C when forging began (the centre being some 150°C hotter). The results in Table II suggest that the tube material would reach about 600°C if the tube were twice as large as the forging (i.e. T/β ratio = 2). Making allowance for the ingot being square, and since the tube would be only intermittently exposed to the radiant heat during forging, it was decided to use a 15-in. dia. tube on the free side of the press. On the manipulator side of the press, the segments were shaped to 9½ in. radius, to allow for height adjustments of the manipulator; the segments could be placed so as to make up a 19-in. dia. tube between press and manipulator extending for 1 ft, 2 ft, 3 ft, and 4 ft respectively. In this way, the part of the forging between the press and manipulator was always fully shielded. When the manipulator approached the press, each section in turn was retracted.

Forging procedure

To assess the advantage of using the reflector assembly, three bars, each 8-in. square × 18 in. long, with tagged ends for gripping, were cogged with the reflectors assembled and compared with two bars cogged in the normal way. The bar surface temperature was 1050°C at the start of forging. All passes began at the tagged ends of the bars and each operation consisted of a predetermined schedule of squeezes and movements between strokes.

During each test a record was taken (*a*) of the time in which the surface temperature of the forging cooled to 900°C (optical measurement), and (*b*) the time which elapsed before the hydraulic press stalled and forging had to cease. The number of forging passes was also recorded and after forging the final extension of each bar was measured and the amount of forging reduction calculated. These observations are set out in Table III.

Discussion

The basic experiments show that cooling of hot steel

TABLE III Results of forging tests

Forging temperatures:	Initial surface temperature 1050°C (optical) centre 1200°C ± 10° (thermocouple) Final surface temperature 850°C ± 20° (optical)				
	With reflectors			Without reflectors	
Test no.	1	2	3	4	5
Time elapsed before surface cooled to 900°C, min s	7 29	8 41	7 42	5 25	5 35
Time elapsed to start of pass on which press stalled, min s	9 55	12 11	12 15	5 7	7 42
No. of pass during which press stalled	17	19	17	13	15
Forging ratio obtained	3·15	3·5	3·15	2·5	2·8

can be retarded quite appreciably by surrounding it with reflectors and that aluminium sheet is a suitable material to use. All ferrous materials were discarded because of the risk of the formation of oxide films but nickel alloys, titanium, and other materials might be suitable if found on investigation to possess adequate heat reflection characteristics.

The forging tests show that it is a practical proposition to use reflectors during forging operations. The test results stated in Table III show wide scatter but they indicate a tendency for the cooling of the steel during forging to be retarded by a factor of about $1\frac{1}{2}$. A greater forging ratio was also obtained. With the assembly used in the laboratory there was a 50% increase in forging time. This means that a forging at present requiring three heats could probably be completed in two heats, which represents a considerable saving, in view of the high cost of heating.

A rudimentary system was tried in the laboratory to prove the principle. A more refined technique is, however, possible. In forging, the hot steel cools and is reduced in cross-section. A cooler bar permits the use of a smaller tube, as does a smaller bar. The two factors taken together may warrant replacing the assembly at intervals with progressively smaller tubes and tube segments in order to conserve as much heat as possible during forging. The most important factor is to ensure that the reflectors are as close as is practicable to the material being shielded throughout the process.

WIDER APPLICATIONS FOR REFLECTORS

Whilst the investigation has been concerned with forging, reflectors may well find use in other processes. A simple tunnel, for example, might be used to cover the part of a forging extending outside the furnace during a reheating operation with benefits both in fuel saving and in reduced temperature gradients along the forging. Slow cooling treatments might also be carried out in a reflector tube instead of in a furnace.

Looking further afield it is possible to envisage uses for reflectors in rolling operations to increase the reduction possible in each heat, a distinct possibility in the rolling of sheet, for example. Other possible uses may arise during the transportation of hot material; and in such processes as continuous casting or extrusion, reflectors may present a convenient way of controlling cooling rates.

ACKNOWLEDGMENTS

The authors are indebted to the members of the Forging Committee of BISRA for their interest and help in the development and particularly to Mr N. Moore, and wish to acknowledge the advice and assistance of their colleagues.

APPENDIX

CALCULATION OF SHIELDING COEFFICIENTS

Using simplified heat transfer theory, it is possible to calculate the heat loss from a bar cooling in air by summing the heat losses by radiation and convection. Rate of heat loss by radiation:

$$q_r = \sigma\, p_B\, A_B\, (T_B^4 - T_a^4) \text{ Btu/h} \quad \ldots\ldots(1)$$

and by convection:

$$q_c = C A_B \left(\frac{1}{D_B}\right)^{\frac{1}{4}} (t_B - t_a) \text{ Btu/h} \quad \ldots\ldots(2)$$

$$\text{Total rate of heat loss} = q_r + q_c \quad \ldots\ldots(3)$$

To calculate the heat loss from a bar inside a tube, however, it is necessary to know beforehand the temperature of the tube. The first step in tube cooling calculations therefore is to estimate tube temperatures and a relatively simple numerical method was used assuming equilibrium between tube and bar, the rate of heat transfer by radiation from the bar to the tube being equal to the rate of heat loss by radiation plus convection from the tube to the surroundings i.e.:

$$\sigma F_e A_B (T_B^4 - T_T^4) = \sigma p_T A_T (T_T^4 - T_a^4) + C A_T \left(\frac{1}{D_T}\right)^{\frac{1}{4}} (t_T - t_a)^{\frac{5}{4}} \quad \ldots\ldots(4)$$

By numerical calculations using equation (4) a range of tube temperatures is assumed and the associated bar temperatures predicted. This gives a table of bar and tube temperatures for any given tube–bar combination.

Notation

q	rate of heat transfer, Btu/h
T	temperature (Fahrenheit scale)
σ	Stefan-Boltzmann constant = $0{\cdot}174 \times 10^{-8}$ Btu/ft² h
p	emissivity of surface (= 1 − reflectivity)
A	Surface area, ft²
t	Temperature, °F
C	convection constant
D	diameter, ft
F_e	a factor to allow for departure of surfaces from blackness $= \dfrac{1}{\frac{1}{p_B} + \frac{A_B}{A_T}\left(\frac{1}{p_T} - 1\right)}$ for 2 concentric cylinders

Suffixes B bar, T tube, c convective, r radiative, a ambient.

The next step in tube cooling calculations is to calculate the radiative heat loss from a bar at a given temperature by calculating the heat radiated to the tube having interpolated the associated tube temperature from the results already obtained:

$$q'_r = \sigma F_e A_B (T_B^4 - T_T^4) \quad \ldots\ldots(5)$$

Finally, if the tube is open-ended, convective heat loss from the bar is added as equation (2) above.

$$\text{Total rate of heat loss from bar in tube} = q'_r + q_c \quad \ldots\ldots(6)$$

The rates of heat loss from a bar when cooling in air and in the tube are then compared to give the coefficient of shielding.

$$\text{Coefficient} = \frac{\text{Rate of heat loss from bar in air}}{\text{.. .. .. in tube}} = \frac{q_r - q_c \text{ equ. (3)}}{q'_r - q_c \text{ equ. (6)}}$$

On this basis the curves A_1 B_1 and C_1 of Fig.2 were computed. The emissivity of the aluminium tubes (curves A_1 and B_1) were assumed to be 0·1 (reflectivity 0·9) and of the stainless-steel tubes (curve C_1) 0·2 (reflectivity 0·8). Curve A_1 was calculated assuming zero convection in the tube, curves B_1 and C_1 assuming a value of C (in equations 2 and 4) as 0·225.[5] The calculated results are reasonably close to those obtained experimentally despite wide divergencies in tube temperature estimates and this simple method of calculation has other disadvantages:

1. Extensive numerical work is involved.

2. Emissivities of bar and tube must be assumed (the effort involved in measuring these was not considered worthwhile for this purpose). Values assumed were (*a*) mild-steel sheet (hot rolled) 0·8,[3] (*b*) rolled aluminium sheet 0·1,[3] and (*c*) lightly oxidized stainless steel 0·2.[4]

3. Convective effects are only approximated (*a*) the empirical equation used is not proven for the high temperature level of the tests but refinement was not considered necessary in the circumstances, and (*b*) the extent and nature of convection currents within a tube were not examined and probably vary from the 'natural convection' on which the formula is based.

4. The simple theory which was used assumes both bar and tube are infinitely long, whereas in the cases examined they are comparatively short.

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Experimental semi-automatic forge

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INTRODUCTION

AUTOMATIC EQUIPMENT is less common in forging than in the mass-producing industries mainly because of the wide range of forgings normally made and the non-repetitive nature of forging production. This paper is concerned with developments in semi-automatic forging, aimed at reducing physical effort and increasing productivity, which have been based on appraisals of the design and use of plant engaged in normal production.

In mechanized forges, a press (or hammer), used for deforming the hot steel, works in conjunction with a manipulator (or crane) handling the material. At present, the two machines are designed independently and controlled separately so that their performance characteristics are not matched and there are difficulties in synchronization. Moreover, the manipulating equipment is often used for transporting the work material as well as for forging, which results in low utilization of the press or hammer. For example, during works investigations, it was found that a 2000-ton press was idle for 20% of the working time whilst its associated crane was being used for transportation, and in the case of a 1000-ton press, the idle time was 66%, most of which was attributable to its manipulator being similarly engaged.

The actual forging operation can only continue whilst the material is hot. The heating process accounts for about half of the total forging cost (in one 5-ton hammer forge it was as much as 80% when cogging Wortle steel ingots). Thus, delays during forging or

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SYNOPSIS

An experimental forge comprising a 200-ton press and rail-mounted manipulator has been built as part of a programme of research into the forging operation, and the development of automation of the process.

The paper describes the improvements made to press performance by the installation of modern hydraulic valves, and details of the design of the manipulator which is matched to the press for maximum operating speed and efficiency. Semi-automatic operation of the forge unit has been achieved by co-ordination of the movement of the press and manipulator.

Finally, the method of working with this plant is indicated and a brief account given of the plant performance.

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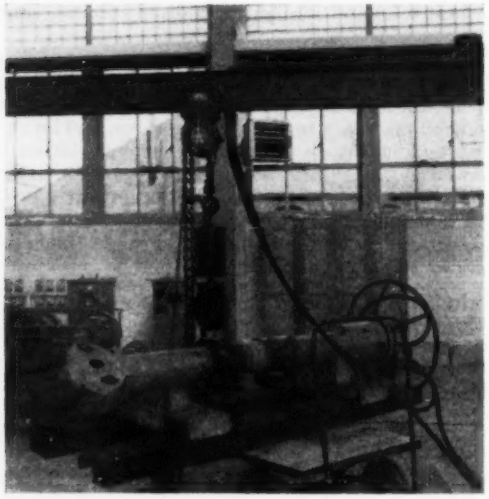


1 *Manually operated peel*

slow plant operation can result not only in low output but also in uneconomic production because of extra reheating of each forging.

Manipulators are in general slow in operation; for example a 1000-ton press has been found to be working at half its potential stroking rate because the manipulator did not keep pace with it. In cases where the press was slow, our studies indicated the need for improved valve gear and faster pressure release and build up in both main and lifting cylinders.

Considerable scope for improved methods of control was revealed by the investigation of industrial forges. In particular, manual control of presses seems to be associated with an excessive number of forging strokes being used. On one 2000-ton press, in an operation which consisted of 92 press strokes, over half the strokes were being used for finishing to the correct size. A similar effect was found in the same forge, which was attributable to manual control of manipulation. In one finishing operation consisting of 226 strokes, the movement of the forging between strokes varied from 2 in. to 12 in.; had the movements been regular at 11 in. each time, only 135 strokes would have been required. It was also noted that the rate of working was affected by the time taken to position a forging between each stroke. For example, in forging the end journal of a roll the average time between press strokes may well be double the time between strokes during cogging when there is less need for accurate placing.

Summarizing, the study of forging operations indicated these needs: first, a close connection between press and manipulators both in the design stage and in their controls; secondly, high-speed machines; and thirdly, improved techniques of control of both press stroking and manipulator movements. To study possible methods of obtaining such improvements it was decided to develop a forging plant on a laboratory scale.

PROGRAMME

A 200-ton press, normally used for other research, was available in the laboratory, together with a manually operated peel with hydraulic gripping jaws which was slung by a chain from an overhead beam when used for manipulating forgings (Fig.1). Although the press was slow in operation it appeared possible to improve its performance by detailed attention to the hydraulic circuit and in particular to valve design. The rate of stroking which could be expected after improvement was about 60 strokes/min during cogging and this was accepted as the standard to be aimed at.

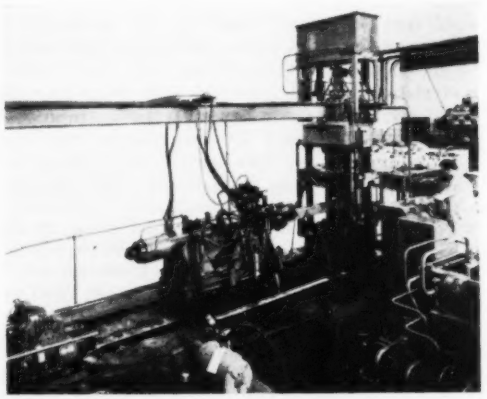
To eliminate hand manipulation it was decided to build a rail-mounted manipulator to carry the existing peel (Fig.2). The manipulator was intended to be used solely to handle forgings at the press so obviating delays caused when manipulating machinery is also used for shop transport. For simplicity the machine was designed to impart only three motions to a forging, namely, rotational, vertical, and longitudinal. It was not considered necessary at this stage to incorporate other motions required for complex operations. To ensure that the manipulator would keep pace with the press the specification was based on a forging rate of 60 strokes/min which meant that longitudinal and rotational motions of 3 in. and 30° respectively should be made in $\frac{1}{3}$ s. The manipulator was required to handle forgings up to 10 in. square and weighing 6 cwt; the maximum length of forging provided for was 8 ft.

A general objective of the work was to devise suitable control devices which would overcome the limitations of manual control. The main requirement was for positional control of the press tool and of the three motions of the manipulator. A second requirement was for a linking of the press and manipulator controls to give co-ordinated operation of the two machines. It was also intended to investigate the use of programmed control to preselect the desired movements of both press and manipulator automatically.

THE PRESS HYDRAULIC CIRCUIT

The press is served directly from four double pumps, each with a high- and low-pressure side. The low-pressure pumps are automatically unloaded before maximum pressure is reached, in order to avoid excessive overload on the electric driving motors.

The original circuit delivered oil to alternate sides of the ram by action of a control valve with three positions, i.e. forge, neutral, and return. The control valve was in turn moved by a solenoid-operated pilot valve.



2 *General view of BISRA manipulator and press*

The press was slow in operation, the principal delays being an interruption of about 0·5 s in the build-up of pressure during the penetration, and the time required (0·4 s) to build up pressure in the annulus for the return motion. In addition, both the free approach and lifting speeds were slow, being just over 3 in./s. No effective prefilling during the approach stroke was obtained, and only one high-pressure pump was used for lifting.

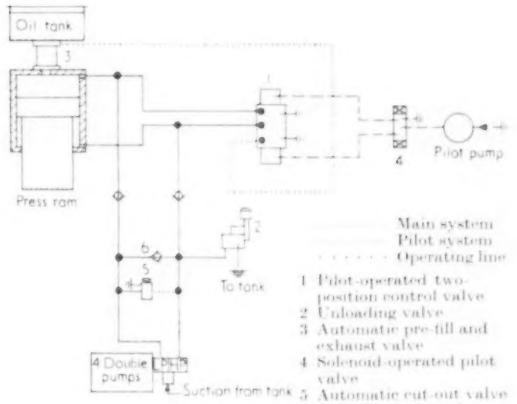
To improve the performance of the press, a new control system was installed at reasonable cost, since no modification to the press itself was required, and the existing main pumping plant was retained (*see* Fig.3). Solenoid pilot valves were again used to link up with the electric position control.

The main features of the new hydraulic controls are a two-position control valve and a special prefill and exhaust valve. In the one position of the control valve, oil from all four high-pressure pumps is directed to the annulus below the ram and to the exhaust-valve operating cylinder, so that the ram moves up at a speed of 10 in./s; the output of the low-pressure pumps passes back to tank via the press cylinder and the prefill and exhaust valve. If the pumps are unloaded with the control valve in this position, the oil in the annulus is trapped by a non-return valve, and the ram is supported in a stationary position.

In the other control valve position, the pumps are connected to both sides of the ram, and the ram moves down. The loss of load brought about by using only the differential area of the ram has been offset by raising the maximum pressure from 5000 lb/in² to 6000 lb/in². An improvement in the rate of penetration has been brought about by raising the cut-out pressure of the low-pressure pumps from 2500 lb/in² to 4500 lb/in². The tank is situated immediately above the press cylinder, with only the new automatic prefill valve between. This arrangement gives a fast free-approach stroke at a speed of 14 in./s without having an air-loaded tank. The delays in pressure build-up previously occurring have disappeared. The new circuit also completely eliminates the previous delay of 0·1 s between the end of the approach stroke and the beginning of pressure build-up, and decompression time is cut by half to 0·06–0·08 s. The pressure built up in the annulus during the forging stroke ensures an immediate start of the return stroke as soon as decompression is complete.

As a result of these modifications the stroke cycle time has been cut from just over 3 s to between 0·9 and 1·1 s for the press alone allowing for a free approach stroke of 2 in. and a penetration of 1 in. If the pressure in the cylinder is above 4500 lb/in² throughout penetration, the longer time applies, whereas, if only a low force is needed and the low-pressure pumps do not unload during forging, then the rate of penetration is higher and the cycle quicker. In practice, a cycle time between 0·9 and 1·1 s occurs.

With the old arrangement, the manipulator was waiting for the press, but with the new system the free stroke of the press must normally be at least 2 in. to allow sufficient time for the manipulator to complete its motion or alternatively a short deliberate dwell may be introduced at the top of the stroke. Valve operation is simplified if the press is kept oscillating continuously.



3 *Press hydraulic circuit as modified*

THE MANIPULATOR

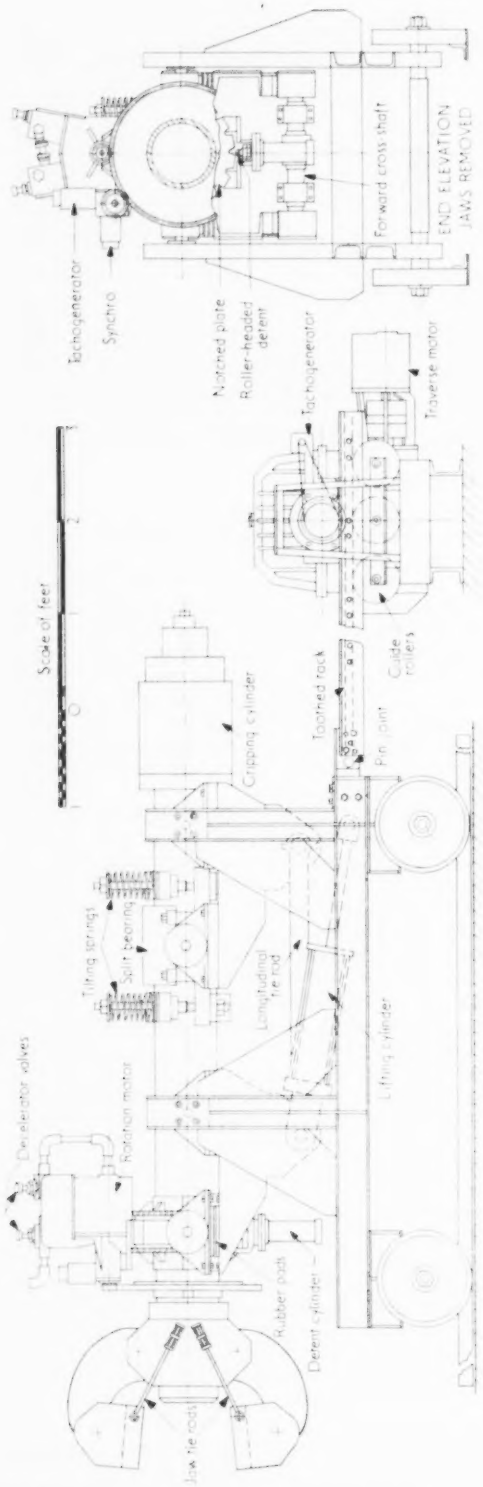
The original peel was suspended by a chain, and the first requirement was to design a suitable carriage which would be capable of rapid movement and respond accurately to control. Many mechanical systems, as, for example, ropes and pulleys, cannot respond accurately to rapid positional control signals. Generally a mechanical system to have good response must have a minimum of yield or compliance under the forces required. If a large mass has to be moved the connections must be correspondingly stiff. The use of large volumes of oil for control of an inertia load is equally undesirable on account of the compressibility of the oil.

A survey of existing commercially available manipulators* had shown that they are of two general types, rail mounted and mobile. Mobile manipulators are usually required also to serve the function of furnace chargers and it was decided that to get the best results, it would be necessary to limit our machine to one purpose, and depend on the crane for transport to and from the furnace. A straight line rail-mounted carriage was therefore decided upon.

Normally, a rail-mounted manipulator supports all the machinery necessary for its own propulsion thus unnecessarily increasing its size and weight. It was considered that to reach the accelerations required for traversing, some departure from existing practice would be necessary, and this led to the solution of a fixed external drive, which, besides cutting the weight of the carriage, would be independent of any limitation of acceleration arising from slip of the driving wheels on their rails.

In deciding on the best type of motors to use for peel movements, where weight and size would be critical since these would be mounted on the carriage, and for propulsion, electric motors appeared at first to be the obvious choice since the position control would in any case be electrical. Comparison of the weight, size, and torque of available electric and hydraulic motors showed, however, that it would be better to employ hydraulic machinery for all movements, and to accept

* P. L. Lillywhite: *JISI*, 1958, **190**, 394–413.



4 *Forging manipulator, general arrangement*

the disadvantage of having to provide flexible high-pressure piping from the power supplies to the carriage and between the carriage and the peel itself.

CONSTRUCTION

The above considerations led to the adoption of a light but rigid carriage directly connected to the carriage traversing motor. In the interests of economy and speed of construction the design was based upon the use of standard components wherever possible. The carriage which supports the peel is simply constructed of channel sections welded to form a rectangular frame, with suitable box stiffeners.

The general arrangement of the machine as built is shown in Fig.4. Very little modification to the original design was needed and the machine has survived some severe handling. Designed for a single purpose, it is lighter than conventional machines. The total weight of the carriage and peel is 30 cwt, for carrying an ingot weighing up to 6 cwt and up to 8 ft in length.

The manipulator has the following principal modes of movement:

(i) carriage traversing; operated by floor-mounted gear-type motor
(ii) peel rotation; operated by a gear-type motor driving through a single reduction gear to the peel
(iii) peel lifting; operated by a follow-up hydraulic servo cylinder
(iv) automatic peel tilting; by springs, on removal of the press load.

For all the powered motions, position and velocity feedback signals from rotary potential dividers, synchros, and tachogenerators were required, and these introduced many problems of design that had to be solved before satisfactory operation was obtained.

Carriage

For traversing the carriage is coupled to a rack-tooth drawbar. This drawbar engages with a pinion which is driven by a gear-type hydraulic motor mounted on a worm gear box fixed to the floor.

One of the problems in design was to ensure stability of the carriage under maximum acceleration in either direction. The position of the centre of gravity in relation to the wheels and the line of action of the drawbar had to be confined within certain limits. Arising out of these requirements it was found that the forward bearing would have to be of the minimum possible overall dimensions. Roller races and cages were out of the question, quite apart from their cost. It was clearly desirable to avoid the use of a split bearing, so that the smallest diameter would have to be big enough to pass over the end of the peel. The solution adopted was to use plain mild-steel rollers running directly on the peel, entirely filling the space between peel and outer race, molybdenum disulphide grease being used as a lubricant. It is emphasized, however, that crowded races are not in general recommended as they can be very troublesome unless the rollers are prevented from tilting in relation to the axis by closely fitting end flanges.

To guard against the effect of any very heavy longitudinal thrust that might develop from any cause, the carriage is given the overall protection of reversibility in the worm gear drive, and relief valves in the hydraulic supply to prevent fluid lock.

Peel

The peel rotates in two trunnion bearings each carried on a pair of swinging bell crank levers. The levers are carried by bearings mounted on vertical stanchions fixed to the carriage. Cross-shafts and longitudinal tie rods connect the respective pairs of bell cranks and the forward cross-shaft receives the thrust of a hydraulic ram which takes the whole weight of the peel and controls its height. For rotating the peel, a toothed gear ring, concentric with and clamped to the peel, is engaged by a pinion driven by a gear-type hydraulic motor. The motor is mounted on pads provided on the forward main bearing.

The weight of the peel with ingot of maximum length is balanced on the forward main bearing, to which the peel is fixed by clamped rings. These rings transmit thrust between the peel and the outer race of the bearing, and thus to the carriage. There is a 1-in. clearance in the rear bearing, which allows the peel to tilt against the restraint of four helical springs. These springs are designed to hold the ingot clear of the anvil until the press pushes it downwards, the height of the peel being so adjusted that it is horizontal when the forging stroke is completed.

A problem arose from the possibility of severe external loads being applied by the press, caused by elongation of the ingot during penetration, or failure of the crosshead to lift before a movement of the manipulator is started, or by eccentric loading causing twisting of the ingot in the jaws. It was decided that some flexibility of mounting would be necessary until more experience was gained, and so rubber shear pads were provided under the forward trunnion bearing supports. In action, any horizontal force deflects the rubber pads and the peel slides horizontally through the after bearing, the rubber pads providing sufficient damping to prevent undue oscillation. Although a very slight yield against twisting can take place by compression of one of these rubber pads, this is not their main purpose, and, as will be seen later, other steps had to be taken to overcome difficulties due to twisting.

CONTROL FEATURES

Lifting motion

The hydraulic ram used for lifting the peel is of the 'follow-up' type in general use as a steering booster for heavy motor vehicles. In this application the cylinder itself must sustain the weight of the peel and ingot. The links and levers operating the control valve had to be so arranged that in the event of failure of the oil pressure they would 'slip' without being damaged. The method adopted was to use a motor-driven screw thread which is engaged by a spring-loaded ball carried in a sleeve. The sleeve actuates the cylinder control valve, and as the screw is turned, the sleeve, the valve, and the booster cylinder move together.

Peel rotation

Of the three motions of the peel, rotation was found the most difficult from the point of view of accurate control, due to variations in inertia of different sizes of ingot, and to the inevitable existence of a dead band in the on–off position control system. Some form of mechanical detent or lock was required which would ensure accurate positioning and yet yield under excessive twisting forces arising from the press. A conventional manipulator design usually includes a slipping clutch in the rotation drive, and the original intention here was to rely on the hydraulic relief valves to allow the peel to drive the motor as a pump. When it was realized that positive indexing would be required, provision for slip had to be considered afresh.

The solution was to fit notched circular plates each side of the forward trunnion bearing and a hydraulic cylinder slung from the trunnion housing operating a roller-headed detent engaging with the notches. Fluid is supplied at constant pressure when the detent is engaged so that it can retract by sliding out of the notch under the action of a force of sufficient magnitude. As soon as the external force is removed the detent re-engages and restores the angular position of the peel. The notches are spaced at $22\frac{1}{2}^{\circ}$ around the periphery and are each wide enough to cover the angular dead-band error of the control. The notched plate can be seen in the end elevation, Fig.4. In operation, the detent is automatically engaged when the rotational control valve closes. Alternatively it can be cut out entirely.

Manipulator grips

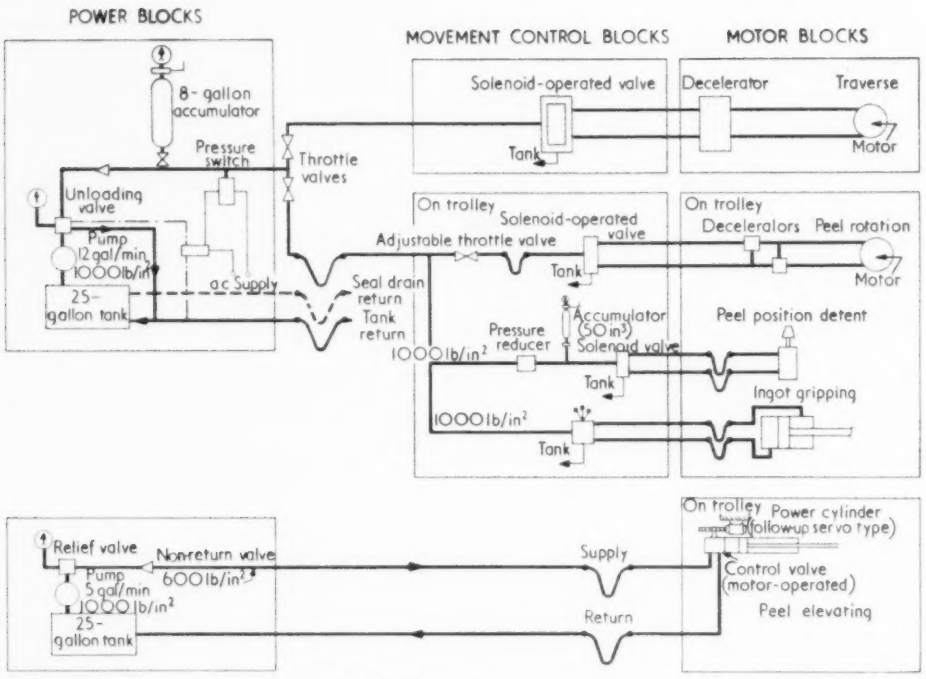
If the gripping jaws were free to turn about their pivots, they might tilt out of line just before a grip is made. The tie rods shown in Fig.4 are so arranged to preserve the alignment of the grips within prescribed limits. Provided the ingot is correctly aligned with its end inside the jaw pivots a satisfactory grip is always assured. This is particularly important where uninterrupted speed of operation is required.

HYDRAULIC CIRCUIT

The circuit is shown diagrammatically in Fig.5, and it will be seen that a separate pump has been provided for the lifting motion, both power blocks being, of course, mounted on the floor. An important point about the main power supply is that the pump is automatically unloaded when the pressure in the accumulator rises above the maximum working pressure. This effectively prevents undue variations in oil temperature, and has greatly reduced the initial difficulties due to speed variation.

Initial calculations showed that, though momentary forces required were high, the power supply could be quite moderate if backed up by a hydraulic accumulator. The original arrangement, which very nearly succeeded, included one $7\frac{1}{2}$-hp 12 gal/min pump at 1000 lb/in^2 for supply of all the motive power. The only reason for departing from this simple system lay in the diverse characteristics of the various actuators. Whereas the control valves of the traversing and rotation motors would shut off completely when not fully open, the valve controlling the lifting cylinder, being of the underlap type, caused a considerable flow of fluid at all positions. Sudden movements called for still greater flows, and at times the demand on a system as a whole became too great. The danger point would be reached if the pressure fell low enough to allow the gripping jaws to release the ingot.

The traversing and rotation motors are each controlled by solenoid-operated valves, which give quick response. To protect the motor and driving gears against a sudden pressure build-up on stopping, decelerator valves are fitted which open at any setting



5 *Forging manipulator, diagrammatic arrangement of hydraulic circuit*

desired, thus by-passing the oil from the high to the low-pressure side of the motor. They are set at a pressure just above the normal maximum working pressure.

The oil supply to the gripping jaws is taken from the main circuit, the hand-operated valve being mounted on the carriage.

The oil supply and return between the power blocks and the carriage are taken along an overhead beam and connected to the carriage via flexible pipes.

REMOTE POSITION CONTROL SYSTEM

It was decided at an early stage that in the interests of economy a simple on–off position control would be acceptable, and this principle was applied throughout the development of both press and manipulator, since these were to be linked together.

It is at times asserted that since the load during a forging stroke is viscous rather than inertial, a simple limit stop is all that is needed to achieve uniformity of repetitive strokes of the press. Since, however, there may be considerable variation in speed of penetration, between different passes, the delay between operation of the stop and cut-off of fluid pressure will result in different distances travelled by the crosshead during the delay period. To achieve independence of velocity, therefore, a velocity feedback appeared necessary, and this has been incorporated in the remote position control system. Velocity signals are also used on the manipulator to give anticipatory switching off in advance of the required stopping point.

The controls for the press and the three motions of the manipulator are similar in principle. The difference between voltages representing the desired and actual positions of a member is amplified and used to actuate the solenoid valves in such a manner that the resulting motion reduces the difference. Cut-off occurs when the difference is less than a prescribed small value. The press control circuit is provided with two calibrated input potential dividers on which the required crosshead positions can be set, and the circuit switches from one to the other alternately, causing the crosshead to reciprocate between the two set positions. Either single strokes or repetitive motion is available as required. The peel rotation circuit turns the peel through a preselected angle each time the controls are operated; the angle may be 15°, 22½°, 45°, or 90°. In a similar manner the long-travel controls move the manipulator away from the press by 1 in., 2 in., 3 in., or 4 in., after the initial position has been selected by means of switches. The lifting control has only a calibrated potential divider on the control desk, and the peel height follows any change in the setting.

Co-ordination of press and manipulator is arranged by triggering the long travel and/or rotational movement of the manipulator each time the press reverses at the bottom of a stroke. Sufficient manipulation time is allowed with short strokes by the introduction of an automatic delay between successive press strokes.

Further information about the control systems has been given elsewhere.*

METHOD OF WORKING

Only one man is needed to operate the manipulator and press together when making not only simple forgings, such as square and round bars, but also more complex shapes of variable cross-section and size. This

* L. J. Chant and F. Seredynski: *JISI*, 1959, **191**, 185–187.

is because all motions of the manipulator and of the press tool are controlled automatically. The operator does not need to judge the extent of any motion by eye but merely selects appropriate positions on dials; the motions then being controlled by the automatic equipment.

For example, when making square bars the forging is normally turned through 90° after each pass. The operator selects the 90° control setting and initiates the motion; then the control gear starts the hydraulic motor and stops it after the peel has turned through roughly 90°, and the mechanical detent moves into the appropriate notch on the peel to correct the angle. In a similar way, should an actagonal section be required, the rotational control can be set for a 45° turn. The operator is not required to estimate the angular movement nor to 'inch' the forging into the correct position. If the forging is required to move towards the press to a fixed position before the start of each pass, one control setting is retained throughout the whole process and the manipulator moves forward each time the operator initiates the movement.

The accuracy of carriage positioning of $\pm \frac{1}{4}$ in. is satisfactory for most operations, although a further inaccuracy sometimes arises due to deflections of the peel relative to the carriage. In general, however, the conventional and rather lengthy process of measuring and marking the forging can be dispensed with.

In order to start a forging pass the operator has to set on dials the upper and lower limits between which the tool will move, adjust the peel height by means of a dial, set the initial position of the long-travel movement of the manipulator, and the amount of movement to occur after each stroke by means of switches, the initial angular position of the peel by means of a dial and also switch to the required amount of angular movement, if any, to take place after each stroke. The only motions that take place during these settings are the peel height adjustment and peel initial angular setting; the long-travel movement takes up its starting position on pressing a push button.

On pressing the start button for the press, the press makes its stroke movements, which in turn start the step-like movements of the manipulator. A button will stop the press at the top of its stroke, which in turn stops the manipulator.

For the next pass, if the conditions are different, the appropriate control settings then need to be reset before the pass can start.

During all passes the press and the manipulator work automatically, the press tool reciprocating between preset limits and each reversal at the end of a forging stroke triggers an incremental movement of the manipulator which may be longitudinal, angular, or a combination of both. The movement between successive strokes is predetermined and thus the number of strokes in any pass is controllable and wastage of strokes due to erratic manipulator movements is eliminated. Each press cycle and manipulator movement takes place at the full operating speed of which the plant is capable and there is no need for cautious positional adjustments of either press tool or manipulator which are a source of waste in conventional plant. The number of finishing strokes can be minimized as the finished size can be approached with more confidence than is normal, as a result of precise control of the press tool during each stroke.

PLANT PERFORMANCE

The plant in operation meets the specification laid down and a normal longitudinal forging pass with 1-in. penetration can be made at the rate of about 60 strokes/min. With 2 in. of press tool free travel the ingot free time at this stroking rate is 0·35 s. Tests have confirmed that the maximum distance the manipulator can move in this time is about 4 in.; at higher speeds, control becomes unstable. Angular movements of up to 45° can be made in the same period but only to a low order of accuracy. For larger angular movements, the mechanical detent is used and the total time from the initiating signal to completion of the motion and re-engagement of the detent is 1·2 s for 45° and 1·4 s for 90° movements. As these motions normally occur whilst a long-travel movement is being made, as between passes, this delay is relatively unimportant.

The rate of stroking is limited by the time taken by the various hydraulic systems to respond to the electrical signals by which they are controlled. Each reversal of the press tool begins 0·10–0·12 s after the initiating signal is given, including 0·06–0·08 s decompression time. Longitudinal movements of the manipulator, which are triggered by the same signal start after 0·24–0·25 s, which includes 0·20 s for automatic resetting of the potential dividers in the control circuit. Small angular movements require a total of 0·17 s. For normal forging operations, the response time of the manipulator is about equal to the time needed for the tool to clear the stock and has no effect on the performance. However, it limits the stroking rate which can be obtained with small press strokes and modifications are in hand to permit resetting the controls during the penetration stroke to allow faster planishing rates.

The dimensional accuracy of forgings produced on the plant is good despite the high speed of operation. The accuracy is of the same order as the accuracy with which the operator can judge the setting of the press tool control dial (a $3\frac{1}{2}$-in. dia. dial on which 330° represents 13 in. of tool movement). In all tests so far carried out, the finished size has been within $\pm \frac{1}{32}$ in. of the nominal size which normally lies in the 3–6 in. range. During any one pass, however, where the control setting remains unchanged, the variability from stroke to stroke is less than $\pm \frac{1}{64}$ in.

CONCLUSION

It has been found entirely practicable to devise a press and manipulator capable of working together at high speed by improving the operation of an existing press and by building a manipulator to keep pace with it during forging. The automatic controls which were used to co-ordinate the rapid motions of the press–manipulator combination have been found to work with an accuracy and reliability well within that normally required for forging.

The trials of the experimental forge have already shown the considerable advantages which could be expected in present production by the use of fast-acting manipulators and interlinking of the machine controls. Further development is now in hand on means for programming the forging operation as a whole and on devices for the automatic adjustment of programmes in response to variations of the condition of the stock forged.

Heating for forging in a batch-type furnace

P. C. Finlayson and J. S. Schofield

SYNOPSIS

Batch-type heating is one of the most thermally inefficient processes in the steel industry. To determine the factors which affect efficiency, the performance of a modern design of bogie hearth furnace has been carefully analysed. Measurements were taken throughout the heating cycles for a 41·6-ton and a 52·5-ton ingot and heat balances have been calculated at hourly intervals.

It has been deduced from the results that the efficiency of a forge furnace depends upon its shape, size, and material of construction, the burner design and method of waste-heat recovery. The way that the furnace is operated also greatly influences its efficiency. The furnace should be loaded at its designed capacity and the furnace temperature controlled to give the maximum permissible heating rates and the minimum soaking times. The application of furnace pressure and temperature control and fuel/air proportioning control can greatly improve the furnace operation. The performance of different furnace designs may be readily compared using the characteristic curves showing the relationship between heat availability, utilization of heat, and thermal efficiency.

Measurements taken of the temperature existing within the two ingots during the heating cycles have shown that their internal temperature distribution may be accurately predicted by numerical analysis, either from the skin temperature or from the furnace temperature if the heat-transfer properties of the furnace are known. This provides a theoretical approach which may be used in choosing heating rates and soaking times. 1754

INTRODUCTION

A SURVEY of the energy requirements in the iron and steel industry has shown that the reheating of ingots in batch-type furnaces is one of the most thermally inefficient processes in the industry.[1] The energy used in forging represents a vitally important factor in the economics of the many firms devoted solely to forging, because the cost of heating represents a considerable proportion of the cost of the process.

Low thermal efficiency is a characteristic of a batch-type heating process, but even so, the performance found in practice is lower than need be. There is a lack of experimental data on the performance of furnaces, burners, and waste-heat recovery units as applied to forging practice. These factors must be investigated fully before an assessment can be made of the essential features which every forge furnace should incorporate. The best designed furnace cannot achieve a high thermal efficiency unless it is operated correctly at its designed capacity.

As a preliminary study of forge furnace design problems, tests have been carried out on a batch-type furnace of modern design. Its performance has been analysed in terms of heat balances calculated for hourly intervals throughout the heating cycles. The results obtained indicate basic improvements in design that might be applied to forge furnaces in general.

Because of the dangers of fracturing the ingot as a result of internal stresses caused by large temperature gradients, the heating schedules must be rigidly controlled. In the past, heating rates have normally been chosen from experience rather than from any accurate knowledge of the internal temperature distribution within the ingots. A numerical method of evaluating temperature distributions, outlined in this paper, has been verified by measurements taken during the tests. This may lead to a technique for establishing maximum permissible heating rates for various ingot sizes, provided that the limiting temperature gradients for the different steels can be found.

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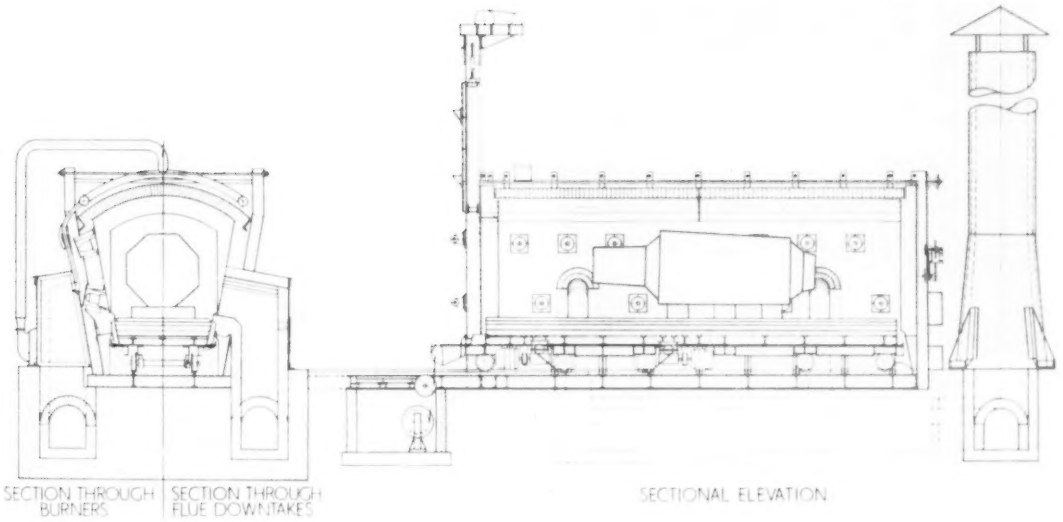
The authors are on the staff of the Energy Section of the Division.

DESCRIPTION OF FURNACE

The tests were carried out on a bogie hearth forge furnace at the works of Walter Somers Ltd. The furnace is oil fired and is capable of heating ingots up to 65 tons in weight. The furnace incorporates several unusual features including the use of high-temperature refractories, water-cooled doors and jambs, and a self-propelled bogie.

The furnace is shown in end and side sectional elevations in Fig.1. The hearth dimensions are 31 ft 6 in.



1 *Bogie hearth forge furnace*

7 ft 6 in., the height to the bottom of the furnace arch being 9 ft 0 in. The heat capacity of the furnace structure has been reduced to a minimum by constructing the furnace largely of hot-face insulating bricks. The side and back walls consist of 9 in. of M_8 high-temperature insulating bricks, 4½ in. of P_3 insulating firebricks, ⅜ in. of asbestos boarding, and ¼ in. of welded steel casing. The furnace arch is constructed of a 9-in. thickness of M_8 high-temperature insulating brick. The door and jambs are insulated and water cooled.

The furnace brickwork does not extend below bogie level, a free space being left for ventilation. It may be seen from Fig.1 how the bogie is driven by two electric motors mounted directly below it.

The furnace is fired by 28 Schieldrop oil burners recessed in the side walls. On each side of the furnace there are 8 burners firing above the ingot and 6 burners firing below. The burners are grouped in four independently controlled zones, the fuel input to each zone being controlled by the temperature recorded by thermocouples located in the furnace walls.

Temperatures tend to fluctuate most at either end of the furnace, the end zones are therefore smaller, each consisting of six burners while the two central zones each have eight burners.

The oil has a viscosity of 950 s Redwood no.1 at 100°F. It is preheated to a temperature of about 210°F and circulated in a ring main surrounding the furnace at a pressure of 15 lb/in². The Schieldrop burners each incorporate a valve which determines the oil/air ratio. The combustion air is supplied at a pressure of 28 in. wg by four fans. Each fan supplies the air required for one zone, thereby eliminating any interaction between adjacent zones.

There are four waste-gas offtakes, two on each side of the furnace. The waste gases pass through underground flues along the length of the furnace. There is no waste-heat recovery, the waste gases being passed directly to the stack. The furnace pressure is automatically controlled using two water-cooled dampers located in the underground flues.

The instrument panel incorporates the pneumatically operated temperature indicating controllers for each of the four zones, the pneumatically operated furnace pressure controller, gauges showing the pressure in the two waste-gas flues and an eight-point recorder giving an indication of the temperature conditions at the walls and roof of the furnace.

MEASUREMENTS TAKEN DURING HEATING CYCLE

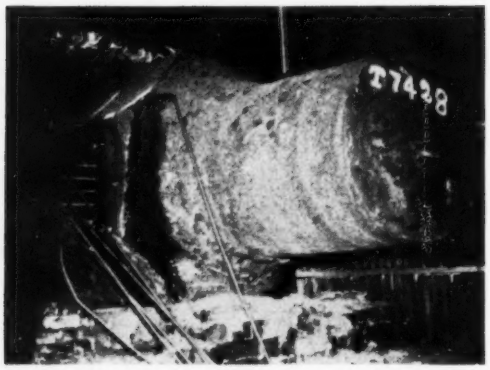
To assess the performance of the furnace, measurements were made of the heat input, and of the way the heat was distributed whilst heating at 41·6-ton and a 52·5-ton ingot up to forging temperature. Heat balances for the furnace were then calculated at hourly intervals throughout the heating cycles. The measurements of the internal temperature distribution in the two ingots provided both a means of assessing the heat stored in the steel at any stage, and also of comparing the measured temperatures with those predicted by theory.

The following measurements were used in the analysis of furnace performance:

(i) fuel input rate
(ii) oil and combustion air temperatures
(iii) waste-gas temperature
(iv) carbon dioxide and oxygen content in waste gas
(v) inside and outside temperatures of the furnace structure
(vi) internal and skin temperatures of the ingots.

Fuel input rate An integrating oil flowmeter, situated at the inlet to the oil heater, was used for measuring the fuel input rate. Readings of the meter were taken at half-hourly intervals.

Oil and combustion air temperatures The oil was preheated to a temperature of about 210°F in order to ensure satisfactory atomization at the burners. The combustion air was not preheated but the temperature of the air rose slightly over ambient conditions in its passage through the fans and hot ducts. Oil and combustion air temperatures were measured at regular



2 *Location of thermocouples in a 52·5-ton ingot*

intervals so that the contribution of the sensible heat in the oil and air to the overall heat input could be assessed.

Waste-gas temperature The temperatures of the waste gases were measured by 5%Rh–Pt/20%Rh–Pt thermocouples situated in the top of the four waste-gas offtakes. The thermocouples were protected from contamination with thin silica sheaths. The temperatures were continuously recorded throughout the heating cycle.

Carbon dioxide and oxygen content in the waste gas The carbon dioxide and oxygen contents of the waste gas were measured at each of the four flues every half hour, the gases being analysed by means of a Fyrite instrument. The carbon dioxide content was used to calculate the percentage of excess air in the furnace chamber.

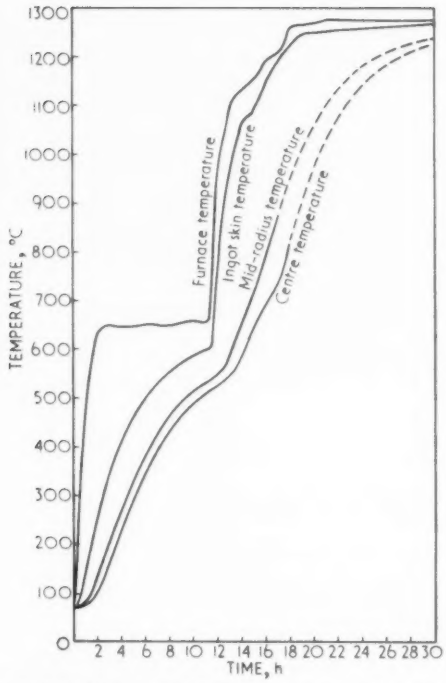
Inside and outside temperatures of the furnace structure There were four Pt/Rh thermocouples sheathed in Inconel tubes permanently located in the side walls of the furnace. These thermocouples were used for the automatic temperature control in the four zones. There were a further four couples projecting into the top of the furnace from the roof. A continuous record was kept of the inside temperature of the furnace as indicated by these eight thermocouples.

The outside temperatures of the side walls and furnace roof were measured at regular intervals using 'Tempilstik' crayons.

Internal and skin temperatures of the ingots Thermocouples were inserted at various depths into the two ingots to determine the temperature distribution within the ingots during the heating cycles. The measurement of the ingot temperatures presented a difficult problem and different techniques were applied in the two tests.

For the test on the 41·6-ton ingot (54-in. octagon), three $\frac{3}{4}$-in. dia. holes were drilled towards the centre of the ingot to depths of 27, 14, and 7 in. A fourth hole 8 in. long was drilled from the shoulder of the ingot, parallel to the skin, at a depth of $\frac{1}{2}$ in. The radial holes were drilled into the ingot at a distance of about 8 in. from the shoulder, close to the line at which the ingot head would later be removed.

Specially designed mild-steel thermocouple plugs were used so that three thermocouples could be firmly

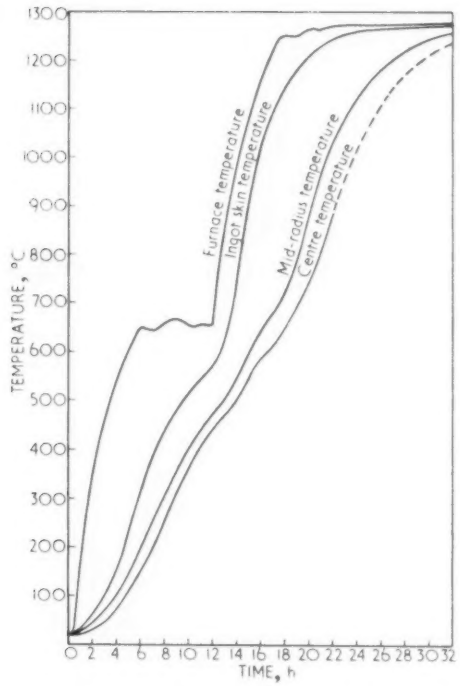


3 *Variation of furnace and ingot temperatures with time during heating of a 41·6-ton ingot*

located at various depths in each of the four holes. This provided a continuous record of the ingot internal temperatures at $3\frac{1}{2}$-in. intervals between centre and skin. Chromel/alumel thermocouple wire protected by twin-bore silica insulators was used. In the furnace chamber, the wire was wrapped with asbestos rope and coated with alumina cement, protected where possible with refractory bricks. This technique of temperature measurement proved to be quite satisfactory at temperatures up to about 900°C. However, at higher temperatures the thermocouple wire became brittle and during the later stages of the heating cycle all the thermocouples failed.

For the test on the 52·5 ton-ingot (58-in. octagon), 5%Rh–Pt/20%Rh–Pt thermocouple wire was used, the wire being protected by twin bore insulators and totally enclosed in Inconel tubing. Four holes were drilled at various depths, perpendicular to one face of the ingot. One thermocouple was inserted in each hole giving temperature measurements at radial distances from the centre of 7·2, 15·5, 22·9, and 27·9 in. A further hole was drilled parallel to the skin at a depth of $\frac{1}{2}$ in. Figure 2 shows the location of the various thermocouples. The Inconel tubing was inserted into the holes, the end of the couples being firmly held at the bottom of the holes by means of tightly fitting plugs. The tubes were bent so that they would lie along the top of the hearth and pass under the furnace door. The temperatures were recorded continuously throughout the heating cycle. This technique of temperature measurement proved more satisfactory although some of the thermocouples failed before the end of the heating cycle.

The skin temperature of the 52·5-ton ingot was

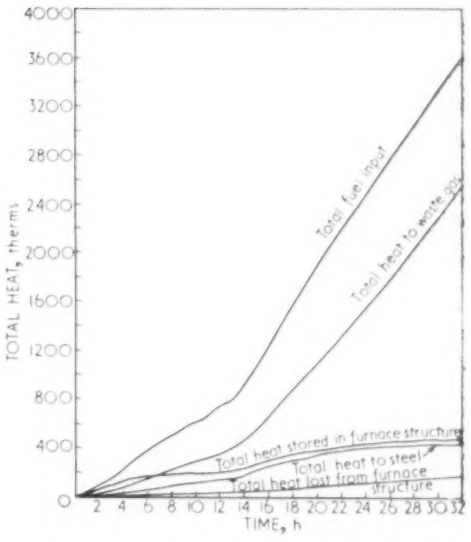


4 *Variation of furnace and ingot temperatures with time during heating of a 52·5-ton ingot*

measured at lower temperatures using a chromel/alumel thermocouple welded to the surface of the ingot, the couple being protected from the flames by a short Inconel sheath. The location of this couple can be seen in Fig.2. At temperatures above 950°C, the skin temperature was determined using an optical pyrometer sighted on the shoulder of the ingot.

OUTLINE OF HEATING CYCLE

The heating schedule normally adopted for this particular furnace and ingot size was applied in both tests. The furnace temperature was taken up rapidly to 650°C and held for a period of about 8 h. The fuel input rate was then increased and the furnace temperature was held at 1275°C for the remainder of the heating cycle. The variation of furnace temperature with time is shown in Fig.3 for the test on the 41·6-ton ingot, and in Fig.4 for that on the 52·5-ton ingot. The intermediate soaking period at 650°C is provided in order to limit the temperature gradients set up within the ingots. It is considered that large temperature gradients are harmful only at temperatures below about 500°C. Once the whole ingot is above this temperature, it may be heated as rapidly as possible without any risk of 'clinking'. The skin, mid-radius, and centre temperatures for the two ingots are also shown in Figs.3 and 4. The dotted portions of the curves indicate calculated values (*see* section on heat transfer) there being no measured values obtained in these ranges because of failure of the thermocouples. The graphs give an indication of the temperature gradients set up within the ingots at the different stages.


TOTAL HEAT, therms
TIME, h
Total fuel input
Total heat to waste gas
Total heat stored in furnace structure
Total heat to steel
Total heat lost from furnace structure

5 *Cumulative record of heat distribution during heating of a 52·5-ton ingot*

When lighting the furnace each of the burners was lit in turn using a propane pilot light. Only 10 burners were lit during the first 12 h of heating. The furnace door was kept open during the initial lighting up period in order to avoid any build-up of unburnt oil vapour in the furnace chamber. Once the door was closed the temperature of the furnace chamber rose fairly rapidly to 650°C, the temperature being maintained at this value by the temperature controllers.

After about 12 h of heating at 650°C, further burners were lit and the furnace temperature was allowed to rise to 1275°C, at which temperature the furnace was again rigidly controlled until the end of the soaking period.

HEAT BALANCE FOR THE FURNACE

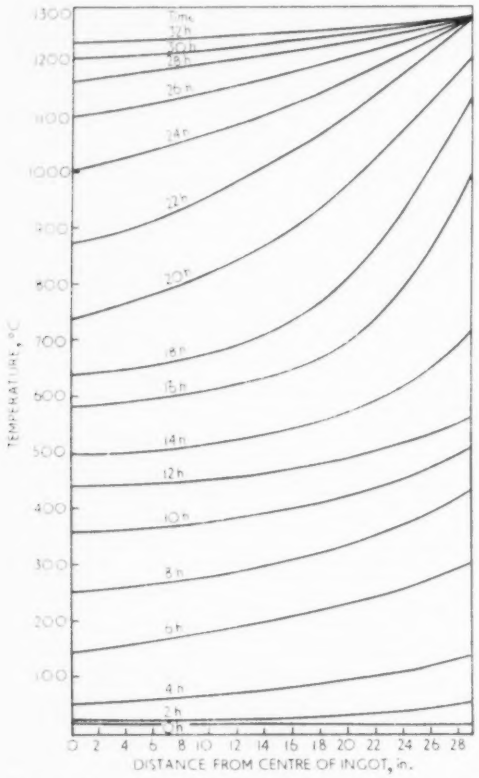
Method of calculation and results

To prepare an hourly heat balance for the furnace, the total quantity of heat entering the furnace has been calculated and balanced against the quantity of heat lost in the waste gases, the quantity stored in the steel and furnace structure, and the amount lost by radiation and convection from the furnace structure. The method of calculation and the results obtained are described below.

Heat input to furnace

The heat input rate is derived directly from the measurements of oil flow. The heavy fuel oil has a net calorific value of 17400 Btu/lb. (Gross C.V. 18600 Btu/lb) and a specific gravity of 0·95 at 60°F. It has been found that the sensible heat content of the air and oil is negligible compared with the heat of combustion of the oil.

A cumulative record of the heat input to the furnace is plotted in Fig.5. It may be seen that the heat input rate was virtually constant for the first 12 h of heating at 61 therms/h (36 gal/h) at which time the furnace



6 *Measured temperature distribution in a 52·5-ton ingot during heating*

temperature was raised to 1275°C. The heat input rate then remained at the higher level of about 151 therms/h (89 gal/h) for the remainder of the heating cycle.

Heat lost in waste gas

The hourly rate of heat loss in the waste gases is calculated from the volumes of the various gaseous components and their temperature. The quantity of excess air supplied is calculated from measurements of the CO_2 content in the waste gas and the composition of the fuel oil which was:

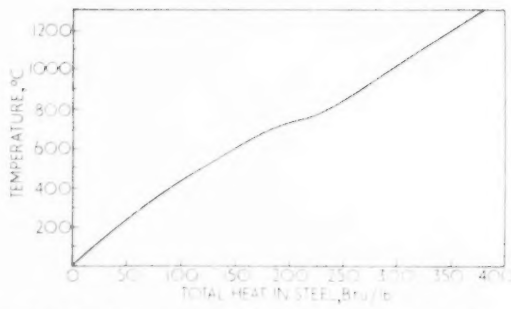
Carbon	85·8%
Hydrogen	11·5%
Oxygen	0·26%
Sulphur	2·4%
Nitrogen, Ash, etc.	0·04%

Because of the wide variation in specific heat with temperature, each of the various components is considered independently and the individual heat contents are summated. A cumulative record of the quantity of heat contained in the waste gas is given in Fig.5.

Heat contained by steel

The measured temperature distribution across the 52·5-ton ingot at two-hourly intervals throughout the heating cycle is shown in Fig.6.

To calculate the heat contained in the steel, the chill part of the ingot is sub-divided radially into eight equally spaced zones. These zones are continued



7 *Total heat/temperature relationship for a 0·23%C steel*

parallel to the ingot skin along both the head and well, the zones terminating at the same equal distances from the ends of the ingot. The masses of the individual zones are calculated from their volumes. It is necessary to assume that the temperature is constant throughout each zone and equal to the measured temperature at the mid-point of the zone. The hourly zone temperatures can then be derived directly from the temperature distribution curves (Fig.6). The relationship between temperature and total heat content of 0·23%C steel is shown in Fig.7.[2] The hourly change in heat contained in the whole ingot can therefore be obtained by summating the changes in heat content of each of the eight zones. A cumulative record of the heat stored in the steel is given in Fig.5.

Heat lost from the furnace structure

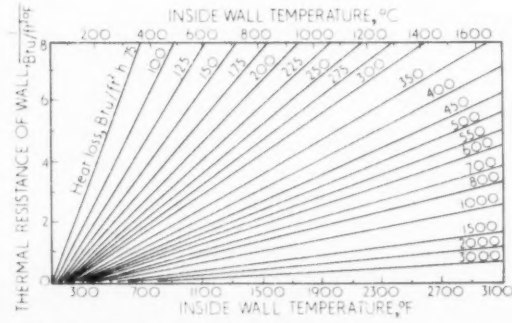
The hourly rate of heat loss from the side walls, back wall, water-cooled door, roof, and underside of bogie were each calculated individually and summated.

Considerable difficulties were experienced in obtaining reliable values for the external temperatures of the furnace structure using the Tempilstik crayons. It was therefore considered to be more satisfactory to estimate the losses from the measurements of the internal furnace temperatures and the calculated values of the thermal resistance of the furnace walls using Fig.8.[3]

The thermal resistance, R, for a composite wall may be calculated from the thicknesses, ℓ, and conductivity k, of the component parts.

$$\text{Where } R = \frac{\ell_1}{k_1} + \frac{\ell_2}{k_2} + \frac{\ell_3}{k_3} + \frac{\ell_4}{k_4} \quad \ldots\ldots(1)$$

The thermal conductivity for refractory materials is temperature dependent and therefore the coefficient of



8 *Heat loss through vertical furnace walls*

TABLE I Variation of thermal conductivity with temperature of various furnace materials

	Side and back walls				Roof	Bogie	Water-cooled door
External area, ft²	700·6				443·4	230·4	109·2
Materials of construction	M_8	P_3	As-bestos	Steel	M_8	Fire-brick	Water-cooled M_8
Thickness, in.	9·0	4·5	0·37	0·25	9·0	15·5	9·0
Thermal conductivity, Btu/ft² h °F/in.							
Mean temperature, °C 100	1·53	2·05	0·37	310·6	1·53	8·85	1·53
250	1·69	2·23			1·69		1·69
500	1·98	2·49			1·98		1·98
750	2·24	2·74			2·24		2·24
1000	2·50				2·50		2·50

thermal resistance must be recalculated for each particular temperature level. Table I shows the variation of thermal conductivity with temperature for the various furnace materials.[4] The figures given by the graph apply only to heat losses from vertical furnace walls. Allowance must therefore be made when calculating the losses from the hot roof,[3] the rate of heat loss being increased by a factor of 1·3. Similarly, the heat loss from the bottom of the bogie will be lower by a factor of 0·6 than the values calculated for vertical walls.

The heat loss from the water-cooled door is calculated, assuming that the cold face of the M_8 insulating bricks is at the water temperature. A cumulative record of the summated heat losses from the furnace structure is plotted in Fig.5.

It was not possible to measure the flow rate of the water used to cool the door jambs. It has therefore been impossible to make any allowance for this loss from the furnace structure.

Heat storage in furnace structure

The storage of heat in the furnace structure may be calculated from values of the mass, specific heat and temperature of the various furnace materials (*see* Table II). As it was not possible to insert thermocouples in the furnace walls, the temperature distribution can only be calculated from the internal wall temperatures and the thermal conductivity of the materials.

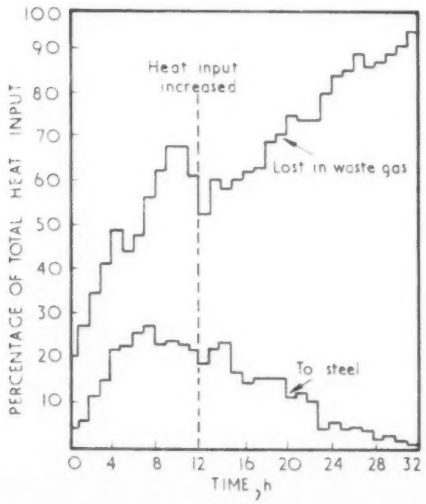
TABLE II Mass and specific heat of furnace materials

	Side and back walls			Roof	Bogie	Water-cooled door
Material of construction	M_8	P_3	Steel	M_8	Fire-brick	M_8
Density, lb/ft³	43	50	490	43	125	43
Mass of material, lb	19410	11250	6263	14205	34560	2750
Specific heat, Btu/lb°F	0·25	0·25	0·12	0·25	0·25	0·25

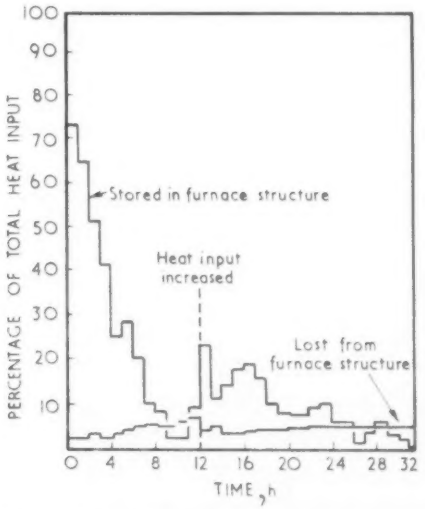
The heat storage in the furnace structure has been approximated by assuming steady state conditions over hourly intervals in the brickwork. The results have been compared with the difference figures obtained by subtracting the heat in the waste gas, steel, and lost from the furnace structure from the heat input to the furnace. The correlation between the calculated values for heat storage and the difference figures is good during periods when the furnace temperature is steady, but poor when the heating rates are high. As it is considered that the difference figures more accurately represent the amount of heat stored in the furnace structure, these values are plotted in Fig.5. It should be noted, however, that the values also include any unaccounted losses.

The calculated heat balance for the furnace

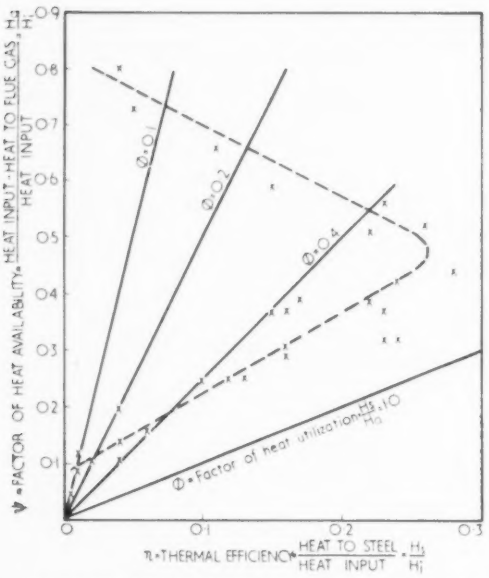
The performance of a furnace is normally presented in terms of an overall heat balance, the total heat input throughout the heating cycle being balanced against the heat in steel and heat losses.



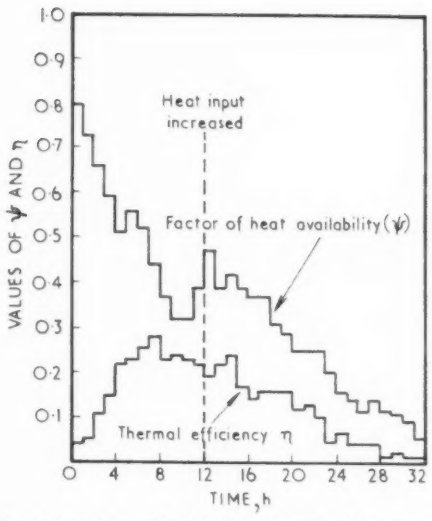
9 *Percentage of total heat input in steel and waste gas during hourly intervals in the heating of a 52·5-ton ingot*



10 *Percentage of total heat input stored in or lost from furnace structure during hourly intervals in the heating of a 52·5-ton ingot*


ψ = FACTOR OF HEAT AVAILABILITY = HEAT INPUT − HEAT TO FLUE GAS / HEAT INPUT = H1 − H2 / H1
η = THERMAL EFFICIENCY = HEAT TO STEEL / HEAT INPUT = H3 / H1
Φ=0·1
Φ=0·2
Φ=0·4
Φ = Factor of heat utilization H3 − H0 / H1 − H0
0·1
0·2
0·3
0·4
0·5
0·6
0·7
0·8
0·9
1·0

11 *Relationship between thermal efficiency and factor of heat availability during heating of a 52·5-ton ingot*



12 *Variation of thermal efficiency and factor of heat availability during hourly intervals in the heating of a 52·5-ton ingot*

For the batch-type bogie hearth furnace, the overall heat balance for the 32-h heating cycle was as follows:

Heat in waste gas	70·8% of heat input
Heat in steel	11·5% of heat input
Heat lost from furnace structure	4·8% of heat input
Heat stored in furnace structure and losses unaccounted for	12·9% of heat input
Total	100%

The performance of the furnace is, however, more clearly shown by calculating separate heat balances at hourly intervals throughout the heating cycle. These data are given in Figs.9 and 10.

The figures show how the furnace structure rapidly stores heat during the first 7 h of heating and also for 6 h after the furnace temperature is increased, the amount of heat taken up during the remainder of the heating cycle being small. The proportion of heat transferred to the steel reaches a maximum of 28% during the eighth hour of heating and then falls off rapidly during the later stages.

The percentage heat loss from the furnace structure remains approximately constant at about 5% of the heat input throughout the whole heating cycle. The heat lost in the waste gas progressively increases until during the last hour of the heating cycle 95% of the heat input is being lost to the waste gases. For the whole of the final 13 h of heating, the waste-gas loss exceeds 70% of the total heat input.

ANALYSIS OF MEASUREMENTS MADE ON THE FURNACE

The performance of a forging furnace is normally expressed in terms of the ratio heat in steel (*Hs*)/heat input (*Hi*), which is the thermal efficiency, η, of the furnace. However, it has been suggested by Southern[5,6] that the furnace performance is better considered in terms of the amount of heat available in the furnace (*Ha*).

Where Heat available = Heat input − Heat in waste gas

$$Ha = Hi - Hg \quad \ldots\ldots(2)$$

Now the proportion of heat available for use in the furnace is expressed by the ratio:

$$\psi = \frac{Ha}{Hi} = \frac{Hi - Hg}{Hi} \quad \ldots\ldots(3)$$

where ψ is the factor of heat availability.

Only a part of the available heat is transferred to the steel whilst the remainder is stored in or lost from the furnace structure, *Hl*. This is expressed by the ratio:

$$\phi = \frac{Hs}{Ha} = \frac{Ha - Hl}{Ha} \quad \ldots\ldots(4)$$

where ϕ is the factor of heat utilization.

From equations (3) and (4) it may be seen that

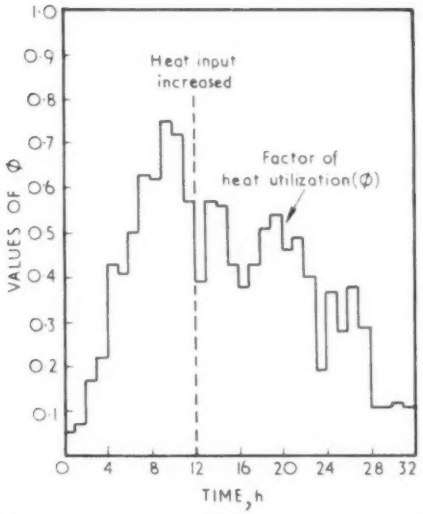
$$\eta = \frac{Hs}{Hi} = \frac{Ha}{Hi} \times \frac{Hs}{Ha} = \psi \cdot \phi \quad \ldots\ldots(5)$$

To achieve a high thermal efficiency for the furnace, the values of both ψ and ϕ must be as near as possible to unity.

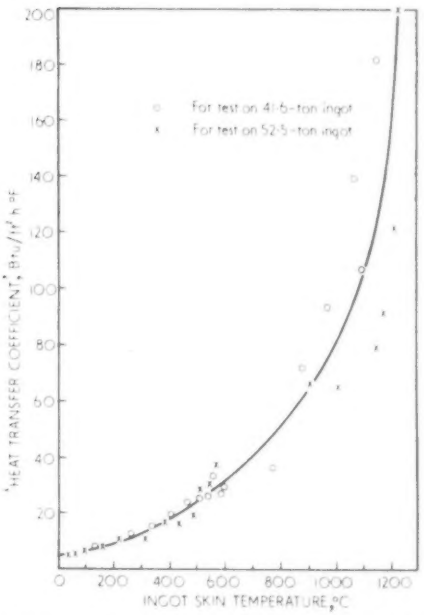
The variations of ϕ, ψ, and η during hourly intervals in the heating of a 52·5 ton ingot are shown in Figs.12 and 13. The relationship between η and ψ is shown in Fig.11, with ϕ being shown as a parameter.

During the early stages of the heating cycle, most of the heat is being stored in the furnace structure and so the values of ϕ are very low. The temperature of the waste gases also tends to be low and thus the values of ψ are high. The efficiency of the furnace is therefore low during the initial stages, the values of ψ, ϕ, and η lying at the top of the dotted line in Fig.11.

As the heating proceeds, the proportion of heat entering the furnace structure quickly falls off (*see* Fig.10), whilst the proportion of heat entering the steel, as indicated by the value of ϕ, rises to a maximum after 8 h. As the furnace temperature rises, so also the waste-gas losses increase, with a consequent reduction in the values of ψ. The thermal efficiency of



13 *Variation of factor of heat utilization during hourly intervals in the heating of a 52·5-ton ingot*



14 *Variation of heat transfer coefficient with ingot skin temperature*

the furnace tends to rise as a result of this increase in ϕ. Therefore, during the heating period up to 8 h, the values of ϕ, ψ, and η lie along the upper portion of the dotted line in Fig.11, the values of maximum efficiency being achieved towards the end of this period.

As the heating proceeds and the ingot temperature approaches the furnace temperature, so the rate of acceptance of heat by the ingot falls off. This is shown in Fig.13 by the reduction in the values of ϕ with time. As a result of this reduction in rate of acceptance of heat by the steel and furnace structure, the waste gas losses rise rapidly with a consequent falling off in the values of ψ. The thermal efficiency therefore also tends towards zero near the end of the heating cycle. The values of ϕ, ψ, and η are thus represented by the lower portion of the dotted line in Fig.11.

The relationship between ϕ, ψ, and η for a batch heating process can therefore be satisfactorily represented by this one curve shown in Fig.11. The values lie on the top portion of the curve at the start of the heating cycle, then change progressively down the curve with the length of time from the start of heating. The shape of the curve will depend on the furnace design. When considering furnace operation, the aim must be to keep close to the peak efficiency as shown by this curve, for the maximum length of time in the heating cycle.

The application of waste-heat recovery

The factor of heat availability is dependent on the fraction of the heat input lost in the waste gases where $\psi = 1 - Hg/Hi$. Therefore for ψ to be high, Hg must be at a minimum.

The amount of heat leaving the chamber will depend on the furnace and burner design and on furnace operation. The heat balances at hourly intervals (*see* Fig.9) have shown that the heat loss in the waste gas progressively increases up to a maximum of 95% of the heat input at the final stages of the heating cycle. There is therefore an obvious need for the application of some form of waste-heat recovery unit, which can bring some of this large quantity of heat back into the furnace in the form of preheated air.

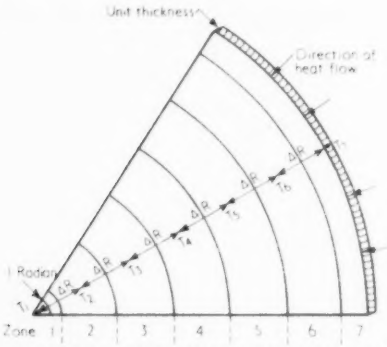
Waste-heat recovery is difficult to apply in conjunction with batch-type furnaces. The amount of waste heat available varies considerably throughout the heating cycle and therefore a recovery unit must be used which can utilize the heat efficiently whenever it is available. Some type of metallic recuperator, having a low heat capacity, would therefore appear to be the most suitable heat recovery unit.

The application of hot face insulation

The factor of heat utilization is an indication of the proportion of the available heat which is transferred to the steel. The remainder of the available heat is stored in, or lost from the furnace structure, where $\phi = 1 - H\ell/Ha$. Therefore in order that ϕ may tend towards unity, the heat losses from the furnace and the heat capacity of the structure must both be low.

Figure 10 shows the amount of heat stored in or lost from the furnace structure during hourly intervals in the heating cycle. The heat losses remain roughly constant at about 5% throughout most of the cycle. The heat capacity of the furnace is low and therefore the proportion of the total heat input stored in the structure is low compared with a conventional firebrick chamber.

Because of the low heat capacity of the furnace and the small wall losses, the values of ϕ are quite high. It may be seen from Fig.13 that for much of the heating cycle over 40% of the heat available in the furnace is transferred to the steel. It seems probable that the values of ϕ would have been much lower for a furnace having a larger heat capacity. This is an indication, therefore, of the improvements in efficiency which are obtained by using hot-face insulating brickwork for furnace construction.



Radius	1/2ΔR	3/2ΔR	5/2ΔR	7/2ΔR	9/2ΔR	11/2ΔR	6ΔR
Surface area normal to heat flow	1/2ΔR	3/2ΔR	5/2ΔR	7/2ΔR	9/2ΔR	11/2ΔR	6ΔR
Volume of zone	$1/8\Delta R^3$	ΔR^3	$2\Delta R^3$	$3\Delta R^3$	$4\Delta R^3$	$5\Delta R^3$	$23/8\Delta R^3$

15 *Method of subdivision of ingot for theoretical calculation of internal temperature distribution*

HEAT TRANSFER TO THE INGOT SKIN

The measurements of the furnace temperature and temperature distribution within the 41·6- and 52·5-ton ingots, shown in Figs.3, 4, and 6, provide a means of assessing the rate of heat transfer from the furnace to the ingot skin. The heat transfer coefficients which can be evaluated from the data provide a useful guide to the furnace performance at various temperature levels.

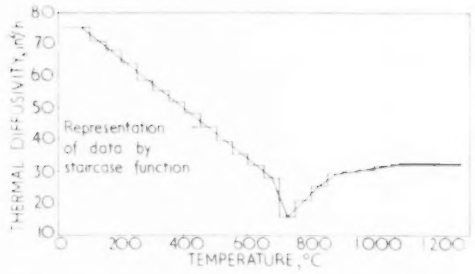
The heat transferred to unit area of ingot surface per second can be written as:

$$h\,(T_f - T_s) = k\left(\frac{dT}{dr}\right)_s \qquad \ldots\ldots(6)$$

where h = heat transfer coefficient for radiation and convection
T_f = furnace temperature
T_s = ingot skin temperature
k = thermal conductivity of the ingot at temperature T_s
$\left(\frac{dT}{dr}\right)_s$ = temperature gradient at the ingot surface.

The measured temperature distribution in a 52·5-ton ingot during heating is given in Fig.6 from which the gradients $(dT/dr)_s$ can be obtained. The corresponding furnace and ingot skin temperatures are shown in Fig.4. Similar curves for the heating of a 41·6 ton ingot were obtained and the furnace and ingot skin temperatures are given in Fig.3. From these data and the conductivity of the steel at temperature T_s, values of the heat transfer coefficient were evaluated and plotted against ingot skin temperature in Fig.14. The heat transfer coefficient rises at an increasing rate because of the increasing effect of radiation from the furnace walls.

In the early stages of heating, the ingot is receiving heat by radiation and convection from the flame and hot gases alone and the heat transfer coefficients are low. More rapid heating for the same fuel input rate in the early stages would only be obtained by increasing the convection component of heat transfer by promoting a vigorous scrubbing action of the furnace gases on the ingot surface. This high convective heat transfer might be achieved by the application of high-velocity jet burners. It must be remembered, however, that there is a definite limit to the temperature gradients which can be tolerated in an ingot and gradients in excess of this will lead to clinking of the ingot. There is, however, little available information on the limiting temperature gradients which can be tolerated.



16 *Variation of thermal diffusivity with temperature for a 0·23%C steel*

In the later stages of heating, radiation from the furnace walls is the most important mechanism in transferring heat to the ingot skin and the maximum heating rates are obtained by raising the temperature of the furnace as rapidly as possible to forging temperature or slightly above. The use of a furnace lining of hot-face insulating bricks whose heat capacity is low and which are capable of being rapidly brought to the final forging temperature is therefore desirable.

The application of combined gas and oil burners, where high-velocity gas jets would promote good convective heat transfer in the early stages of heating, followed by a high-temperature oil flame to bring the furnace up to forging temperature as rapidly as possible, also seems promising.

Emphasis should be placed on the design of the furnace chamber which should provide rapid circulation of the hot gases around the ingot and uniform radiant heating to all its faces. This should ensure even heating and obviate the necessity of turning the ingot.

HEAT TRANSFER WITHIN THE INGOT

The performance of a forging furnace is dependent upon the ease with which heat is transferred from the furnace to the ingot skin and from the ingot skin into its centre. The heating and soaking times necessary for ingots of different size can only be accurately assessed from a knowledge of the temperature distribution existing in the ingot during its heating cycle. It is impractical in most cases to drill the ingot in order to insert thermocouples to record the internal temperature distribution so that the solution of heating rates and soaking times lies in theoretical calculations.

The calculation of the internal temperature distribution within an ingot during heating is a problem in unsteady-state heat conduction. Numerous mathematical texts on the subject are available[7–11] so that the programming of heating and cooling schedules ought to be passing from an empirical to a calculable procedure.

Hunt[12] has outlined a numerical method for the solution of the internal temperature distribution in a heated ingot which involves a knowledge of the initial temperature distribution in the ingot, the variation of the ingot skin temperature with time, and the physical properties of the steel.

It is the object of this section to outline this analysis

TABLE III Example of coefficients used in the numerical analysis where $\Delta R = 28{\cdot}43/6 = 4{\cdot}738$ in. and $\Delta t = 1/15$h

Temperature interval, °C	Diffusivity, α, in²/h	M	Equation for	Coefficients of T_7	T_6	T_5	T_4	T_3	T_2	T_1
20–100	73·33	4·47	T'_6	0·246	0·553	0·201				
			T'_5		0·252	0·552	0·196			
			T'_4			0·261	0·553	0·186		
			T'_3				0·280	0·552	0·168	
			T'_2					0·335	0·553	0·112
			T'_1						0·895	0·105
100–150	70·87	4·75	T'_6	0·231	0·580	0·189				
			T'_5		0·237	0·579	0·184			
			T'_4			0·246	0·579	0·175		
			T'_3				0·263	0·579	0·158	
			T'_2					0·316	0·579	0·105
			T'_1						0·842	0·158

and examine the accuracy of the temperature distribution predicted by this method by comparing the results with the experimental values obtained in the tests on large ingots during heating. It is also possible to deduce the internal temperature distribution in the ingot from a knowledge of furnace temperature and heat transfer coefficients at the ingot surface as derived in the previous section.

Numerical analysis of the internal temperature distribution in the ingot during heating

Figure 15 shows the geometrical subdivision of the ingot into seven zones each of unit thickness. The areas normal to heat flow and volumes of each zone are shown below the diagram.

A number of assumptions are involved in the calculations which are:

(i) the whole of the ingot surface is uniformly at T°C and the absorption of heat is symmetrical

(ii) The ingot is assumed to be cylindrical and infinite in length so that the heat flow is radial, i.e. there is no heat input to the ends of the ingot

(iii) the initial temperature distribution in the ingot and variation of skin temperature with time is known

(iv) each zone is sufficiently small so that a single temperature can be assigned to it and over a small time Δt the difference of temperature between adjacent zones is regarded as constant.

Consider two adjacent zones j and i and let:

T_j = initial temperature of zone j
T'_j = temperature of zone j after time Δt
T_i = initial temperature of zone i
T'_i = temperature of zone i after time Δt
k_{ji} = thermal conductivity from zone j to zone i
A_{ji} = cross-sectional area of heat flow path between zone j and i
ℓ_{ji} = length of heat flow path from zone j to zone i
c_i = specific heat of zone i
ρ_i = density of zone i
V_i = volume of zone i
α_i = thermal diffusivity of zone i

A heat balance on n zones adjoining i gives:

Net heat flow into zone = heat capacity of zone × temperature rise of zone in time Δt.

$$\sum_{j=1}^{n} \frac{k_{ji} A_{ji}}{\ell_{ji}} (T_j - T_i)\,\Delta t = c_i \rho_i V_i (T'_i - T_i) \quad \ldots\ldots(7)$$

The quantities k_{ji}, c_i, ρ_i are temperature dependent but by suitable selection of temperature range these quantities can be regarded as constant and k_{ji} can be replaced by k_i. The ratio $k_i/c_i\rho_i$ is the thermal diffusivity, α_i, of region i. The relationship between thermal diffusivity and temperature is plotted in Fig.16[2] and the values used in the analysis are represented by the staircase function over 50°C intervals. Equation (7) then simplifies to

$$\sum_{j=1}^{n} \frac{A_{ji}}{\ell_{ji}} (T_j - T_i) = \frac{V_i}{\alpha_i \Delta t} (T'_i - T_i) \quad \ldots\ldots(8)$$

Applying the general equation (8) to zone 1, Fig.15, we have:

$$\text{Zone 1} \quad \frac{\frac{1}{2}\Delta R}{\Delta R} (T_2 - T_1) = \frac{\frac{1}{2}\Delta R^2}{\alpha_1 \Delta t} (T'_1 - T_1) \quad \ldots\ldots(9)$$

$$\text{Zone 2} \quad \frac{1}{\Delta R} (T_3 - T_2) - \frac{\frac{1}{2}\Delta R}{\Delta R} (T_2 - T_1) = \frac{\Delta R^2}{\alpha_2 \Delta t} (T'_2 - T_2) \quad \ldots\ldots(10)$$

writing the dimensionless ratio $M = \dfrac{\Delta R^2}{\alpha \Delta t}$ equations (9) and (10) become:

$$\text{Zone 1} \quad T'_1 = \frac{4}{M} T_2 + \left(1 - \frac{4}{M}\right) T_1 \quad \ldots\ldots(11)$$

$$\text{Zone 2} \quad T'_2 = \frac{3}{2M} T_3 + \left(1 - \frac{2}{M}\right) T_2 + \frac{1}{2M} T_1 \quad \ldots\ldots(12)$$

Similarly for :

$$\text{Zone 3} \quad T'_3 = \frac{5}{4M} T_4 + \left(1 - \frac{2}{M}\right) T_3 + \frac{3}{4M} T_2 \quad \ldots\ldots(13)$$

$$\text{Zone 4} \quad T'_4 = \frac{7}{6M} T_5 + \left(1 - \frac{2}{M}\right) T_4 + \frac{5}{6M} T_3 \quad \ldots\ldots(14)$$

$$\text{Zone 5} \quad T'_5 = \frac{9}{8M} T_6 + \left(1 - \frac{2}{M}\right) T_5 + \frac{7}{8M} T_4 \quad \ldots\ldots(15)$$

$$\text{Zone 6} \quad T'_6 = \frac{11}{10M} T_7 + \left(1 - \frac{2}{M}\right) T_6 + \frac{9}{10M} T_5 \quad \ldots\ldots(16)$$

It may be seen that the dimensionless ratio $M = \dfrac{\Delta R^2}{\alpha \Delta t}$ is temperature dependent, since it is a function of the thermal diffusivity of the steel, α.

Now rewriting equation (11).

$$\frac{M}{4} = \frac{T_2 - T_1}{T'_1 - T_1} \quad \ldots\ldots(17)$$

for radial heat flow $T_2 > T'_1 > T_1$

This equation gives the limiting value of M, which must be greater than four. These values of M are also applicable in equations (12)–(16). The maximum time interval of iteration is therefore given by $\Delta t = (\Delta R)^2/4{\cdot}01\,\alpha$.

Δt can be chosen smaller than the value given by this expression with consequent improvement in the accuracy of the calculation. The coefficients of temperatures T_1, T_2, ... T_7 in equations (11)–(16) are

TABLE IV Selected portion of the calculated temperature distribution in a 52·5-ton ingot, dia., 56·9 in.

Line	Time,h	Surface T_7	T_6	Temperature, °C T_5	T_4	T_3	T_2	Centre T_1
46	3·0	96	73·3	57·6	46·9	40·1	36·4	35·1
47	31/15	99	75·7	59·5	48·4	41·4	37·5	36·3
48	32/15	102·5	78·2	61·4	50·0	42·7	38·7	37·4
49	33/15	105	80·8	63·4	51·6	44·1	39·9	38·6
50	34/15	109	83·3	65·5	53·3	45·5	41·2	39·8
51	35/15	112	86·0	67·6	55·0	46·9	42·4	41·0
52	36/15	115	88·7	69·8	56·8	48·4	43·8	42·3
53	37/15	119·3	91·4	72·0	58·6	50·0	45·2	43·6
54	38/15	122	94·4	74·3	60·5	51·6	46·6	45·0
55	39/15	126	97·1	76·7	62·4	53·3	48·1	46·4
56	310/15	128	100·1	79·0	64·4	55·0	49·6	47·9
57	311/15	132	102·6*	81·5	66·5	56·7	51·2	49·4
58	312/15	136	105·4	83·9	68·6	58·5	52·8	51·0
59	313/15	139	108·4	86·3	70·7	60·4	54·5	52·6
60	314/15	143	111·3	88·8	72·9	62·3	56·3	54·3
61	4·0	146	114·4	91·3	75·0	64·2	58·1	56·1
62	41/15	150	117·3	93·4	77·2	66·3	59·9	57·9
63	42/15	154	120·4	96·5	79·5	68·3	61·8	59·7
64	43/15	159	123·6	99·2	81·9	70·3	63·7	61·6
65	44/15	163	127·2	101·8*	84·3	72·4	65·7	63·5
66	45/15	167	130·7	104·6	86·7	74·6	67·7	65·5
67	46/15	172	134·2	107·5	89·2	76·8	69·8	67·5
68	47/15	175	137·9	110·5	91·7	79·1	71·9	69·6
69	48/15	181	141·3	113·5	94·3	81·4	74·1	71·7
70	49/15	186	145·2	116·6	96·9	83·8	76·3	73·8
71	410/15	190	149·2	119·7	99·6	86·2	78·5	76·0
72	411/15	195	152·9*	123·0	102·2*	88·7	80·8	78·2
73	412/15	201	156·8	126·3	105·0	91·2	83·2	80·5
74	413/15	205	161·0	129·6	107·8	93·7	85·6	82·9
75	414/15	211	165·1	133·0	110·7	96·3	88·0	85·3
76	5·0	217	169·4	136·5	113·6	98·9	90·4	87·6
77	51/15	222	174·0	140·1	116·7	101·4*	92·9	90·1
78	52/15	228	178·5	143·8	119·8	104·1	95·4	92·6
79	53/15	234	183·2	147·6	122·9	106·9	97·9	95·1
80	54/15	241	188·0	151·3*	126·2	109·7	100·6	97·6
81	55/15	246	193·1	155·2	129·5	112·6	103·1*	100·2
82	56/15	253	197·9	159·3	132·9	115·5	105·8	102·6*
83	57/15	259	202·9*	163·4	136·3	118·5	108·5	105·3
84	58/15	265	207·9	167·6	139·9	121·6	111·3	108·0
85	59/15	270	213·0	171·8	143·5	124·8	114·2	110·8
86	510/15	276	217·9	176·1	147·2	128·0	117·2	113·7
87	511/15	282	223·0	180·5	150·8*	131·3	120·2	116·6
88	512/15	288	228·1	184·9	154·5	134·7	123·3	119·6
89	513/15	294	233·3	189·3	158·3	138·1	126·5	122·7
90	514/15	300	238·5	193·8	162·2	141·6	129·8	125·9
91	6·0	306	243·8	198·4	166·3	145·2	133·2	129·2

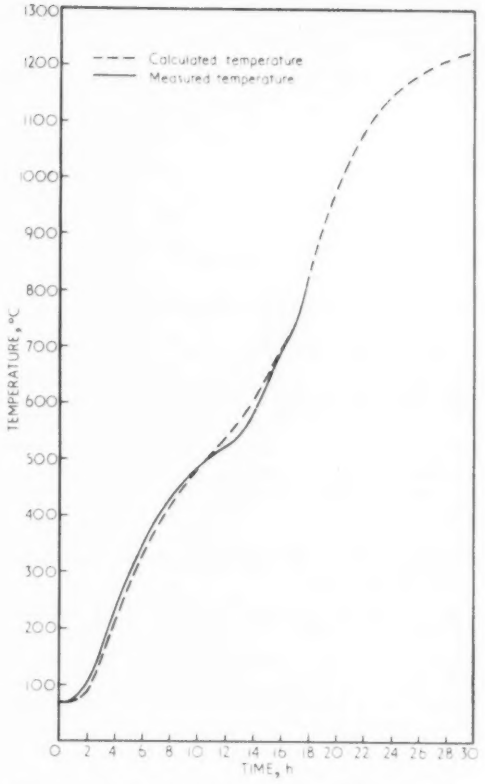
calculated using the appropriate value of M for the temperature interval considered. A typical set of coefficients for the ranges 20–100°C and 100–150°C is set out in Table III. Similar sets of coefficients for 50°C intervals up to 1300°C have also been evaluated.

Starting with the initial temperature distribution and applying the first set of coefficients for the range 20–100°C the temperature distribution at the end of the first time interval is calculated. These values are now regarded as initial conditions for the second interval to which again the appropriate set of coefficients is applied. This iterative procedure is carried forward throughout the complete range of temperatures under consideration. The appropriate temperature range from which to select the coefficients in any calculation is determined by the mean of the temperatures involved in that calculation. The temperature intervals chosen were 20–100°C and then 50°C ranges up to 1300°C. However, it would have been possible to choose larger intervals particularly beyond 800°C without any serious loss of accuracy, since the rate of change of diffusivity with temperature is not large in this region. Other factors influencing the choice of temperature intervals are:

(i) the surface heating rate employed. Short intervals are required when the rate of rise of surface temperature is rapid

(ii) the ingot diameter

(iii) the number of zones used in the calculation.



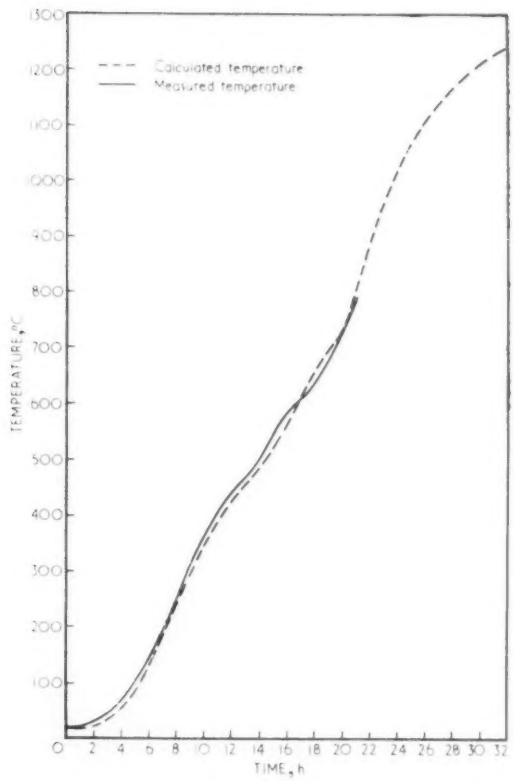
17 *Comparison of calculated and measured temperatures at centre of a 41·6-ton ingot during heating*

The complete analysis of temperature distribution during the whole of the heating cycle occupies 496 lines and is too long to reproduce in full. A selected portion of the calculation of a 52·5-ton ingot is shown in Table IV.

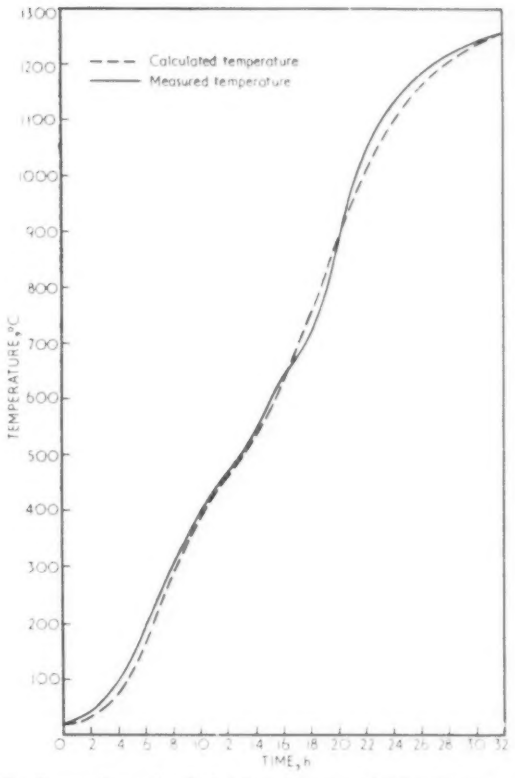
The tabulated values in Table IV up to and including line 55 are calculated with the coefficients for the temperature interval 20–100°C given in Table III. For the calculation of T'_6, line 57, the coefficients are selected from the temperature interval 100–150°C since the mean of T_7, T_6, and T_5 from line 56 lies in this range. This procedure to determine the temperature range from which to select the coefficients was adopted throughout the calculations. The temperatures marked with an asterisk in Table IV represent the first temperature to be calculated with a new set of coefficients.

Comparison of calculated and measured ingot temperatures

In making the theoretical analysis it was assumed that the ingot was cylindrical in cross-section. The two



18 *Comparison of calculated and measured temperatures at centre of a 52·5-ton ingot during heating*



19 *Comparison of calculated and measured temperatures at mid-radius of a 52·5-ton ingot during heating*

tests were conducted on octagonal ingots and therefore the problem arises of assigning a circular section for use in the calculation. For the 52·5-ton ingot which was a nominal 58 in. across flats a theoretical analysis was made using three different diameters. The following diameters were chosen: (*a*) 56·9 in. representing the diameter of the smallest circle touching all eight sides of the octagon, (*b*) 59·6 in. representing the diameter of a circle of equal cross-section to the octagon, (*c*) 58·0 in. a mean between (*a*) and (*b*).

Two surface heating rates were specified for each of the three sizes which were

(i) the temperature obtained by extrapolation to the surface of the values plotted in Fig.6

(ii) the temperature recorded by the chromel/alumel couple welded on to the surface of the ingot and protected from the flames by a short Inconel sheath.

The surface temperature specified from (ii) was found to be in excess of that given by (i) especially in the temperature range up to about 500°C.

Six calculations were completed by the BISRA computer, each of the two surface heating rates being applied to each of the three diameters chosen. It was found from this analysis that the surface heating rate specified by method (i) above gave results which approximated closely to the recorded values; in particular the calculation associated with a diameter of 56·9 in. The comparison of calculated and recorded temperature at the centre and mid-radius of the 52·5-ton ingot is shown in Figs.18 and 19. Figure 17 gives the comparison between calculated and recorded temperatures at the centre of the 41·6-ton ingot. In this case only one calculation was made with a surface heating rate specified by method (i) and a diameter of 54 in. It was possible to record the centre temperature up to only about 800°C in each test because the centre thermocouple failed at this stage. The temperatures at the mid-radius were recorded satisfactorily throughout the heating cycle. The agreement obtained between calculated and measured temperatures is set out in Table V. There is sufficiently close agreement between the measured and calculated temperatures to justify this method of numerical analysis. Therefore by making similar assumptions, it should be possible to obtain reliable data for any size of ingot or heating schedule.

Comparison of predicted soaking times

The method developed by Sarjant and Hartree[13] for the prediction of soaking times involves a knowledge of the time required to raise the surface temperature of the ingot through the last 100°C. A nomogram was developed for this calculation which is reproduced in Fig.20.

A further check on the numerical analysis described in this paper can be obtained by comparing the predicted soaking time with that given from the Sarjant and Hartree nomogram. The construction required in the case of the 52·5-ton ingot which took 13·3 h for its surface to rise from 1170° to 1270°C is shown in Fig.20.

TABLE V Deviation of calculated temperatures from measured temperatures

Ingot size		At centre, °C	At mid-radius, °C
52·5 tons	Mean + ve deviation	+ 14·5	+ 20·7
56·9 in. dia.	Mean − ve deviation	− 11·2	− 11·2
	Maximum + ve deviation	+ 24·1	+ 40·3
	Maximum − ve deviation	− 20·8	− 43·5
	Mean deviation	12·9	17·4
41·6 tons	Mean + ve deviation	+ 12·5	
54·0 in. dia.	Mean − ve deviation	− 9·4	
	Maximum + ve deviation	+ 25·0	
	Maximum − ve deviation	− 16·0	
	Mean deviation	11·9	

The diameter of the ingot is 56·9 in. (point p on the L scale), and the mean diffusivity in this temperature range is 33 in²/h (point q on the D scale). Joining p and q and producing gives point r on the F scale which when joined through the required degree of temperature uniformity, (point s on the C scale) and produced to scale K gives point t. This value is transferred to the second K scale (point u) and u is connected to v, the time for the surface to rise through the last 100°C. Point w at a value of 1½ h is the required soaking time after the surface has reached its final temperature of 1270°C. Figure 4 indicates that at the beginning of the soaking period, 32 h from the start of the test, the centre temperature of the ingot is 1238°C. Calculations predict that this temperature reaches 1251°C after a further 1½ h and the degree of temperature uniformity is therefore 19°C compared with 20°C uniformity at this time given by the Sarjant and Hartree treatment. Thus there is good agreement between the analysis described in this paper and that given by Sarjant and Hartree.

Temperature distribution in the ingot calculated from furnace temperature

Where it is not convenient to measure the ingot skin temperature during heating, the temperature distribution in the ingot can still be calculated from the variation of furnace temperature and the value of the surface heat transfer coefficient. The variation of the heat transfer coefficient with skin temperature is shown in Fig.14. This curve is typical only for the particular furnace and heating cycle and must be determined experimentally.

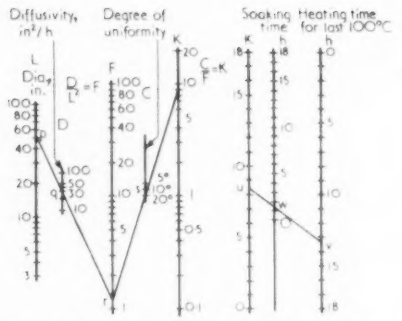
An outline of the method of calculating the ingot temperature distribution from furnace temperature will now be given.

Consider the ingot subdivided as in Fig.15 but into n zones 1, ... $(n-1)$, n so that the ingot radius $R = (n-1)\,\Delta R$

Let T_n = initial temperature of the ingot skin
T'_n = temperature of the ingot skin after a time Δt
T_{n-1} = initial temperature of zone $(n-1)$
other symbols are as given in previous sections.

A heat balance on zone n gives the general equation:

$$(n-1)\,\Delta R\,h\,(T_f - T_n) - (n-\tfrac{1}{2})\,\Delta R\,\frac{k_n}{\Delta R}\,(T_n - T_{n-1}) = \frac{\Delta R^2\,(\frac{1}{2}n - \frac{3}{8})}{\Delta t}\,(c\rho)_n\,(T'_n - T_n) \quad \ldots\ldots(18)$$



20 *Nomogram for the calculation of soaking times*

Now $a = \dfrac{k}{c\rho}$; $M = \dfrac{\Delta R^2}{a\Delta t}$ and introducing the Biot or Nusselt dimensionless ratio $N = \dfrac{\Delta R}{k} \cdot h$ equation (18) reduces to

$$T'_n = \frac{N(n-1)}{M(\frac{1}{2}n-\frac{3}{8})} \cdot T_f + \frac{(\frac{1}{2}nM - \frac{3}{8}M - N(n-1) - n + \frac{3}{4})}{M\,(\frac{1}{2}n - \frac{3}{8})}\,T_n + \frac{(n-\frac{3}{4})}{M(\frac{1}{2}n - \frac{3}{8})} \cdot T_{n-1} \quad \ldots\ldots(19)$$

This general equation can be applied to the particular case of the subdivision of the ingot into 7 zones as shown in Fig.15, when the following equation is obtained:

$$T'_7 = \frac{48}{23}\frac{N}{M}\,T_f + \left(1 - \frac{48}{23}\frac{N}{M} - \frac{44}{23M}\right) \cdot T_7 + \frac{44}{23M} \cdot T_6 \quad \ldots\ldots(20)$$

From this equation, the temperature of the ingot skin, after a short interval of time Δt, can be calculated from the initial conditions of furnace temperature and temperature distribution in the ingot. A table of coefficients of T_f, T_7, and T_6 for different temperature ranges must be evaluated since both M and N are temperature dependent. Equation (20) can now be used with equations (11)–(16) to evaluate the ingot temperature distribution during the whole of the heating cycle.

The scope of the numerical analysis

It has been demonstrated that the analysis outlined is justified in spite of the assumptions made in its derivation. It is capable of providing answers to the unknown elements in any given heating schedule.

Solution of heating problem

The theoretical analysis can for example be used in predicting the unknown heating rates and soaking times in the heating cycle of a 58-in. dia. ingot of 0·23%C steel initially at 20°C. In order to ensure that the ingot is reasonably uniform in temperature during phase transformations and that there is temperature uniformity before forging, the heating schedule is partly defined as follows:

(i) raise the surface temperature at 50°C/h until a surface temperature of 500°C is attained
(ii) raise the temperature of the surface at such a rate that when the surface reaches 550°C the centre is not less than 400°C
(iii) raise the surface temperature at 70°C/h until a surface temperature of 1270°C is attained
(iv) maintain the surface temperature at 1270°C until the centre reaches 1250°C (minimum).

The above schedule leaves the following questions to be answered by numerical analysis:

(i) what rate of heating should be specified in raising the surface temperature from 500°C to 550°C?
(ii) how long should the surface temperature be maintained at 1270°C?

Choice of best heating schedules
Using this analysis it should also be possible to derive heating programmes for different steels and ingot sizes, which set up the predetermined permissible temperature gradients within the ingots. These conditions would ensure that the heat transfer by conduction towards the ingot centre is a maximum.

The permissible internal temperature gradients which any particular ingot can withstand will depend upon the type of steel and its previous thermal treatment. It may be possible to calculate a value for the permissible temperature gradient, at any particular temperature level from a theoretical analysis of the stress distribution during heating.[14] The residual stress existing in the ingot will be additional to the stresses set up by expansion during the heating process. As an alternative approach, it might be possible to obtain data for the heating rates used when ingots have failed under thermal stress. The numerical method of analysis then provides a means for calculating the temperature gradients which existed at failure.

CONCLUSIONS

The factors influencing the performance of a batch-type furnace are most easily considered under the two headings of furnace design and furnace operation.

Furnace design

Size and shape of furnace chamber
The furnace should be designed to promote high convective heat transfer to the ingot skin during the initial stages of heating, when the furnace walls are not radiating appreciably to the ingot. Consideration must also be given to providing even heating in the later stages, when radiation is the major mechanism of heat transfer, by ensuring that all faces of the ingot are receiving equal radiation.

Materials of furnace construction
In batch heating processes where repeated heating and cooling of the furnace structure occurs, it is essential that the furnace should have a minimum heat capacity. This is provided by the use of hot-face insulating brickwork. The strength of this refractory is considerably lower than firebrick and is liable to be damaged if knocked during ingot charging. It can be successfully applied in the construction of bogie hearth furnaces. However, in fixed hearth furnaces mechanical damage is more likely. The thickness of the furnace walls should be chosen from consideration of heat losses from the structure as compared with the heat stored in the structure.

Burner design
In order to promote convective heat transfer, the application of high-velocity jet burners is recommended, particularly during the early stages of heating. Combined gas and oil burners might be used giving good convective heat transfer in the early stages of heating from high-velocity gas flames and radiant heating in the later stages from highly emissive oil flames.

Waste-heat recovery
Unless waste-heat recovery is applied, a large proportion of the heat input is lost in the waste gas irrespective of furnace design or operation. It is therefore imperative to incorporate some form of waste-heat recovery unit so that the amount of heat made available in the furnace chamber can be increased. The waste-heat recovery unit for a batch-type process must have a low heat capacity so that it may utilize efficiently the very variable quantities of waste heat leaving the furnace chamber. A recuperative system of waste-heat recovery would therefore be preferred to a regenerative system.

Furnace operation

Heating cycles and soaking times
The thermal efficiency of a furnace is also influenced by the heating rates and soaking times employed. Taking into consideration the fuel costs, capital costs and labour costs per ton of throughput, it would appear advantageous to keep the length of the heating cycle to a minimum. The numerical analysis of internal temperature distribution within a heated ingot, should lead to a technique for establishing the maximum heating rates, which may be used without risk of damaging the ingot. The soaking times required to give a certain uniformity of temperature can also be accurately estimated.

Furnace loading
There will be a particular loading at which the furnace will operate at maximum thermal efficiency. If the furnace is underloaded, a smaller fraction of the available heat in the furnace chamber will be taken up by the steel and therefore the thermal efficiency will be low. If the ingot is too large for the furnace and the ingot head protrudes through a bricked-up doorway, the losses from the chamber will be increased considerably, with consequent lowering of thermal efficiency. There is therefore the need for careful planning in every forge shop, the designed furnace sizes being carefully chosen taking into consideration the expected range of ingot sizes.

Furnace control and instrumentation
The installation of instruments can greatly assist the correct operation of a furnace, temperature and pressure controllers being the most essential. Furnace temperature controllers limit the amount of fuel wasted during the heating and soaking periods and prevent any damage to the ingot skin due to overheating. If required, programme controllers may be used to raise the furnace temperature at predetermined rates. For a guide to the furnace operator, the temperatures at various points within the furnace should be continuously recorded.

The quantity of excess air passing through the furnace chamber greatly affects the waste-gas losses and consequently the efficiency of the heating process. It is therefore advisable to install a furnace pressure controller which can limit the amount of excess air drawn into the chamber. This is particularly important where a large ingot is being heated with its head protruding from a bricked-up doorway. Furnace pressure controllers are, however, of little use unless

the fuel/air ratio at the burners is also rigidly controlled. This may be accomplished either at each burner by some type of mechanical proportioning device or else the fuel/air ratio to the whole furnace may be accurately set using a proportioning controller.

The thermal efficiency of a forge furnace is influenced by all these different features of furnace design and operation. Their relative importance can only be assessed by experimentation. Therefore, a study of furnace performance is to be carried out by BISRA, using an experimental forge furnace, whose characteristics can be radically altered.

Comparison of furnace performance

It is useful to have some criteria whereby to compare the performance of various furnace designs. The performance of a forge furnace is normally expressed in terms of the overall thermal efficiency of the process. It has been shown in this paper that heat balances over short time intervals show the variation in heat losses much more clearly. It is suggested, however, that furnace performance may be better compared by the characteristic curve of factor of heat availability/thermal efficiency.

The thermal efficiency of a furnace is dependent on both the factor of heat availability and the factor of heat utilization. The factor of heat availability will depend particularly on the furnace design and efficiency of waste-heat recovery. The factor of heat utilization will be influenced by the furnace and burner design including the materials of construction and by the operation and loading of the furnace. Characteristic heat availability/thermal efficiency curves should be obtainable for each furnace, the best furnace being one where the values of the three variables are at a maximum for the longest period of time in the heating cycle.

It should be possible to compare the performance of different furnace designs using this characteristic curve. It may then be possible to determine the relative importance of the various features of furnace design and operation.

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Historical note no. 5 (second series)

Women's labour in the early English iron industry

H. R. Schubert

IT COULD BE EXPECTED that the employment of women in the English industry started with, and was caused by, the general shortage of labour brought on by the Black Death in the middle of the fourteenth century. There is, however, evidence of women's labour for industrial purposes from well before such a date. In 1317 five women were used regularly for carrying brush-wood from marshland by the river Tanner on the border of the counties of Devon and Cornwall to the firm land for charcoal-burning and were paid for their work one penny a day each.[1]

The first illustration of a woman forging iron in an English smithy is in the Holkhan Bible made in south-eastern England. It was painted between 1325 and 1331 and represents a scene from the 'Road to Calvary' which was a favourite motif in religious literature during the Middle Ages. The woman is taking the place of her husband, the smith, who when asked to forge the nails for the cross refused under the pretext that his hand had been injured. When he was told to show the hand his pious lie was made retrospectively true.[2]

In 1408–9 the wives of the three principal ironworkers, i.e. of the foreman, the blomer, and the smith at Byrkenott forge near Bedburn in Weardale, county Durham, assisted their husbands in various ways, such as urging the bellows, crushing the ore, and riddling it with a sieve. They received only a fraction of the wages of male workers. For example, the blomer was paid at the rate of 6*d* for every bloom of iron he produced in the bloom or smelting hearth, but his wife only a halfpenny for each bloom. The payment, however, was not as bad as it looks, since 3*d* per day was considered an adequate sustenance allowance for a man and his horse.[3]

Seasonal lack of the water required for the wheel that urged the bellows was one of the major causes for retarding production in the English charcoal-iron industry.[4] In periods of drought ironworks frequently were forced to close down. A typical example in this respect is supplied by a report dated Portsmouth 8 August 1653. In it Thomas Newberry reported to the Ordinance Office about his journey to various ironmills in the Weald of Sussex and Surrey where he was ordered to make contracts with ironmasters regarding the casting of 'great shot' for Portsmouth. Of seven ironworks he visited only two were able to promise delivery before winter. The others were unable to produce for lack of water.[5]

This was not a single incident, but there are many similar ones reported in history. In February 1744 John Fuller, ironmaster at Heathfield, in Sussex, wrote[6] 'If the weather continueth, want of water will blow out all the furnaces in the county'. He added that for the same reason the furnaces at Beckley, Robertsbridge, and Waldron, in Sussex, could only be operated by treading the wheel, 'which is an excessive charge'.[6]

1 *Osmund furnace**

Treading the large water wheel of the furnace was expensive because it was heavy work. In smaller ironworks a much simpler device could be applied. Instead of treading the wheel to move the bellows, the bellows could be worked by treadles alternately pressed down by the foot of the worker. This could easily be performed by a woman. An illustration of 1732 representing a Swedish Osmund furnace (Fig.1) proves that no heavy work was involved, for the woman who worked the bellows with the help of treadles was at the same time able to spin, the distaff clearly being visible in the picture.

As late as in the eighteenth century, women of English ironworkers frequently were still employed for crushing and riddling the iron ore as they were in 1408–9, and also for picking out pieces of iron and charcoal from among the refuse dump of the blast-furnace.[7]

Even in real smith's work women's labour was not uncommon in the eighteenth century, e.g. in the nail industry of the Midlands, which is shown in a travel report by William Hutton, an early historian of Birmingham. Hutton was surprised by the prodigious number of blacksmith's shops he found. In some of them he observed one or more female workers 'stripped of their upper garments and not overcharged with their lower, wielding the hammer with all the grace of their sex'. When he enquired what they were, he was told they were nailors. 'The beauties of their faces', he went on with his description, 'were rather eclipsed by the smut of the anvil or, in poetical phrase, the tincture of the forge had taken possession of those lips which might have been taken by the kiss'.[8]

[1] John Ryland's Library, Manchester, Latin MS 248.

[2] H. R. Schubert: 'History of the British Iron and Steel Industry, c.450 B.C. to A.D. 1775', 143 (illustration plate XIV, facing p.103); 1957, London, Routledge and Kegan Paul.

[3] G. T. Lapsley: 'The account roll of a fifteenth-century ironmaster', *The English Historical Review*, 1899, **14**, 511, 512, 517, 520–528.

[4] *See* statement of 1593 by Richard Pegg of Shipley near Bradford, Yorkshire, who had been an ironmaster for twenty years in most of the best English ironworks. H. R. Schubert, *loc. cit.* 243.

[5] Public Record Office, State Papers Domestic Interregnum, **39**, fol.62.

[6] Sussex Archaeological Collections, Cambridge, 1926, **62**, 44.

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[8] W. Hutton: The History of Birmingham, 1835, 6th edn., Birmingham.

* Reproduced from J. Percy: 'Metallurgy of iron', 321, Fig.27; 1864, London, John Murray; also O. Johannsen: Geschichte des Eisens', 127, Fig.93; 1953, 3rd edn., Düsseldorf, Stahleisen.

Modification of the cast structure of high-speed steel

G. Hoyle, B.Sc., A.I.M., and E. Ineson, B.Sc., F.I.M.

SYNOPSIS

The eutectic phase normally present in as-cast high-speed steels can be modified by a simple heating operation at a temperature in the region of the peritectic phase change. The treatment, which has been termed 'prespheroidizing', gives well distributed angular particles of eta-carbide which, in the forged, rolled, or extruded condition, give a structure free from carbide stringers. Samples treated by this new technique give satisfactory hardness when hardened by conventional methods.

The process has been applied with success to small sections of various sized ingots and also to cast cutters, but at present there are practical difficulties in extending its use to commercial ingots and large sections.

The technique may offer a method of restoring partially overheated (but not burnt) high-speed steel components.

1755

INTRODUCTION

SINCE THE DEVELOPMENT of high-speed steel, about 50 to 60 years ago, one of the main problems facing manufacturers and users of this material has been the occurrence in the wrought product of carbide stringers and networks whose origin can be traced to eutectic colonies and grain-boundary networks formed during the freezing process. The general attitude towards carbide heterogeneity in high-speed steels is typified in a recent article by Niesielski.[1] Small discontinuous carbide formations may not be of great detriment to the performance of a tool, but severe carbide stringers or any form of carbide network cannot be tolerated since they are believed to be detrimental at all stages of tool steel manufacture, and may give inferior properties in the finished tool. Also, internal bursts, which occur in high-speed steel during the various hot-working processes from the ingot to the tool blank, are often associated with major carbide segregates. Although the problem is particularly acute in connection with the tungsten and molybdenum high-speed steels, similar phenomena are encountered in high-carbon, high-chromium die steels, and Sachs[2] has recently shown that carbide stringers can, in this material, be a contributory cause of distortion in heat-treatment. The ever increasing stringency of steel users' requirements and standards of acceptance has in recent years emphasized the problem and the need for its elimination. The problem has been reviewed by Ineson and Hoyle,[3] who discussed possible methods of alleviating carbide heterogeneity during the freezing process by innoculation, vibration, or electromagnetic stirring of the melt. No practical solution involving any of these techniques has yet been devised by the authors, though it is still believed that the ultimate answer may lie in some modification of the freezing process, coupled perhaps with certain composition stipulations. An independent claim has already been made for the successful application of an innoculation process.[4]

In the meantime a closer study of the constitution of high-speed steels has shed further light on the conditions of carbide eutectic formation. This has led to the development of a heat-treatment process which is capable of dispersing carbide eutectics, though the commercial implications of using such a treatment still require considerable thought.

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THE CONSTITUTION OF HIGH-SPEED STEEL

The extreme complexity of high-speed steel compositions, a typical selection of which is given in Tables I and II, renders fundamental work on the constitution of these alloys very difficult. The basic 18–4–1 types contain major quantities of iron, tungsten, chromium vanadium, and carbon, and other types may contain, in addition, molybdenum or cobalt, or both. It thus becomes virtually impossible to make an accurate representation of the constitution diagram and it is necessary to make approximations from the published diagrams of other simpler systems by making allowance for the effect of additional elements. The most satisfactory basis for such approximations is the Fe–W–C system for which the data of Takeda[5,6] remain virtually unchallenged. A very popular method of 'correcting' the data for use with high-speed steel has been to determine the 'tungsten equivalent' of the additional elements, and it has been stated[7] that a normal 18–4–1 steel may be considered as being similar in constitution to an alloy of iron and carbon with 25%W. This must, however, be taken as a first approximation only, as the presence of the additional elements is known to

TABLE I Compositions of steels used in thermal analysis

Type	C	W	Cr	V	Mo	Co
18–4–1 as-cast	0·72	17·5	4·3	1·3	...	...
,, wrought	0·72	18·4	4·3	1·1	...	...
18–4–1 + 5%Co	0·80	18·2	4·5	1·4	...	5·8
6–5–2	0·82	6·3	4·1	1·9	4·9	...
High-C high-Cr die steel	2·01	...	13·0	...	...	...

TABLE II Prespheroidizing conditions for high-speed steels

Type	Sample no.	Composition, % C	W	Cr	V	Mo	Co	Treatment temperature, °C / Time of treatment, h: 1220 ½	1220 2	1220 4	1230 ½	1230 2	1230 4	1240 ½	1240 2	1240 4	1250 ½	1250 2	1250 4	1260 ½	1260 2	1260 4	1270 ½	1270 2	1270 4	1280 ½	1280 2	1280 4	1290 ½	1290 2	1290 4	1300 ½	1300 2	1300 4	1310 ½	1310 2	1310 4	1320 ½	1320 2	1320 4	1330 ½	1330 2	1330 4	1340 ½	1340 2	1340 4	1350 ½	1350 2	1350 4
18–4–1	538	0·72	17·5	4·3	1·3	...	...																										A	—		B	—		—	—		D	—		E		
	539	0·74	17·5	4·3	1·3	...	...																										A	B	—	B	B	C	—	—		D	E	E	E		
	540	0·71	17·6	4·3	1·2	...	...																										A	B	—	B	D	C	—	—		D	E	E	E		
	541	0·75	18·6	4·2	1·2	...	...																										A	B	—	A	B	C	—	—		D	E	E	E		
	542	0·75	18·1	4·2	1·2																												A	B	—	A	C	D	—	—		D	D	E	E		
	559*	0·74	18·2	4·3	1·3																												—	—	—	A	B	—	D	D							
	560	0·81	18·2	4·3	1·3																												B	B	C	—	—	—	D	D				E			
	561	0·84	18·3	4·2	0·9																												B	C	C	—	—	—	D	D				E			
	562	0·67	18·1	4·3	1·3																												A	B	C	—	—	—	E	E				E			
	563	0·68	19·0	4·5	1·0																												A	B	B	—	—	—	E	C				E			
	564	0·75	18·3	4·7	1·4																												A	B	B	—	—	—	D	C				E			
18–4–1 plus cobalt	565	0·80	18·2	4·5	1·4	...	5·8																																									
	644	0·75	18·0	4·5	1·2		5·0																										B	B	B	—	D	—	—	D	D				E			
	651	0·73	19·7	4·2	1·6	0·61	9·2																			A	—	—	A	—	—	A	—	—	D	—	—	D	—									
6–5–2	532	0·82	6·2	4·2	1·9	4·8					—	—	—				A	C	C				C	E	E				E	—	—				C	4	—											
	533	0·83	6·2	4·1	1·9	5·1					—	D	D				A	A	C				C	E	E				E	—	—																	
	534	0·80	6·3	4·1	1·9	4·9					—	—	—				A	C	C				C	E	E				E	—	—																	
	535	0·82	6·3	4·1	1·9	4·9					—	—	—				A	A	C				C	E	E				E	—	—																	
	536	0·86	6·2	4·3	2·0	5·5					—	—	—				A	A	C				C	C	E				E	—	—																	
W–Mo–V–Co types (high C)	643	1·25	10·9	4·25	3·25	3·75	10·0	A	—	A†	—	—	A	—	—	D	—	—	D	—	—																											
	650	1·33	9·0	4·26	3·52	3·19	8·56	A	—	A	—	—	C†	—	—	D	—	—	—	—	—																											
High-C high-Cr die steel	537	1·83	...	11·84	...	...	...	—	—	—	—	—	C	C			C	E	E		E																											

* Continuously cast. † Optimum conditions

give rise to the formation of new phases in the system, such as vanadium and chromium carbides.

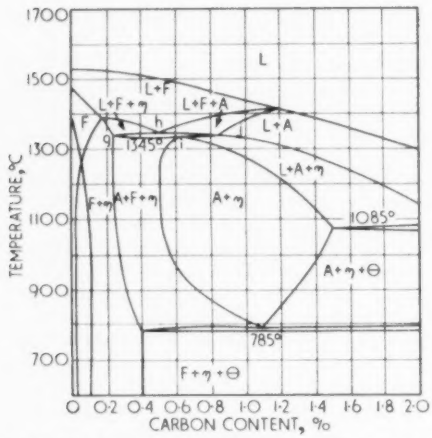
Murakami and Hatta[8] made some progress in extending the scope of available data, and constructed a binary section (at 18%W and 4%Cr) through the quaternary iron–tungsten–chromium–carbon system. Further modifications have since been made to this diagram by Kuo[9] and by Goldschmidt.[10] The latter author's proposals are concerned with low-temperature reactions and it is considered that Kuo's diagram (Fig.1) is the best approximation at present available for a consideration of the factors affecting the freezing of high-speed steels. Thermal analysis data[11] on 18–4–1 high-speed steel suggest that further correction is still required. From the NPL work[11] it would appear that there are two major phase-change points, at 1310°C and 1332°C, on heating. The 1332°C point appears, from the data supplied, to be representative of the peritectic reaction, quoted by Kuo as taking place at 1345°C. In this case the 1310°C point would be expected to represent the change $\gamma + \eta \rightarrow$ liquid + $\gamma + \eta$, or the reaction $\gamma + \eta \rightarrow \gamma + \delta + \eta$. However, it is reported[11] that a specimen quenched from 1305°C showed some blackened particles, indicative of the presence of delta eutectoid[9] and therefore suggesting that this temperature is above the γ-phase boundary. There remains, therefore, a little confusion concerning the system.

A further disadvantage of the diagram in Fig.1 is that no reliable correction can be made for the effect of vanadium. Kuo suggested that allowance might be made for this element by assuming that it forms vanadium carbide stoichiometrically, thereby lowering the 'available' carbon content for reaction according to Fig.1. The problem is by no means as simple as this would suggest. Firstly, it is known that vanadium does not occur exclusively as vanadium carbide: it is known to be present in the eta-carbide phase in high-speed steels[12] and also to enter the matrix. Moreover, it is almost certain that vanadium takes part in a eutectic reaction which, from the observations made during the present research, appears to take place at a lower temperature than the main eta-carbide eutectic reaction. Kuo[13] reviews the relative effects of various elements in ternary equilibrium.

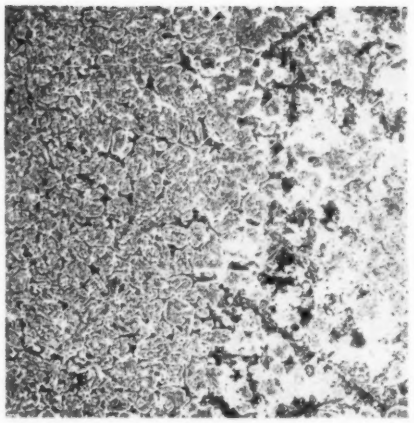
For the purpose of discussion, however, it may be said that Fig.1 gives a qualitative representation of the high-speed steel system, in which the 18–4–1 type (0·7%C) is probably represented by a vertical line in the region of point *i* or slightly to the left or right of this.

THE AS-CAST STRUCTURE OF HIGH-SPEED STEEL

The structure of a typical commercial 18–4–1 steel in the cast and annealed condition has been described previously[3] and is shown in Fig.20*a*. The main constituents present are delta eutectoid, bands of higher alloy content surrounding the islands of eutectoid, colonies of eutectic forming a grain-boundary network and occupying the spaces between crystals, and isolated carbide particles. The simultaneous presence of all these phases is, as pointed out by Kuo,[9] evidence of lack of equilibrium in the system. From Fig.1 it may be deduced that for compositions to the left of point *i* the quaternary peritectic reaction should remove all liquid from the system, thus precluding the formation of eutectic. Similarly, for compositions to the right of *i*,



1 *Constitution diagram of the Fe–W–Cr–C system at 18%W and 4%Cr*



2 *Duplex structure in vibrated 18–4–1 high-speed steel ingot (Figs. 2–4 etched alkaline* $KMnO_4$*)* × 100

the same peritectic reaction should consume all delta-ferrite, thus making liquid available for eutectic reaction on further cooling and at the same time precluding the formation of delta eutectoid.

Apparent confirmation of this view may be obtained from an examination of the structure of ingots subjected to mechanical vibration during freezing, a process which is considered to be instrumental in promoting equilibrium conditions. Vibrated ingots of high-speed steel are generally found to exhibit a severely banded structure and in ingots cast under vibrating conditions by the present authors alternate zones of low (0·7%) and high (1·0%) carbon content have been found. It is probable that alloy segregation also occurs to a considerable extent, but this had not yet been proved. Figure 2 shows that the lower-carbon zones are substantially devoid of eutectic, and the higher-carbon zones are mainly eutectiferous with a complete absence of delta eutectoid. The actual mechanism of formation of the banded structure is not relevant to the present discussion: the explanation advanced by Northcott and McLean[14] appears to explain the phenomena when considered with the constitution diagram.

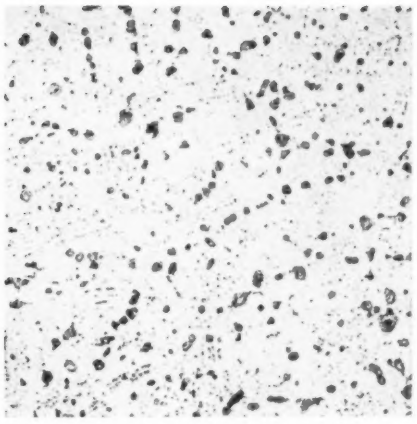
CONTROL OF THE PERITECTIC REACTION DURING SOLIDIFICATION

From the above discussion, it is apparent that the normal structure of high-speed steel in the as-cast condition is representative of a non-equilibrium condition, produced mainly by coring of the primary dendrites and the failure of the peritectic reaction to take place to completion. It is therefore inferred that improved structures may be obtained if this reaction could proceed to equilibrium, at least for the lower-carbon high-speed steels. In a laboratory experiment, a small quantity of 18–4–1 high-speed steel (0·7%C) was re-melted in an alumina crucible supported in a manually controlled tube furnace. After fusion, the steel was cooled in the furnace at a very slow rate, especially through the critical range 1350–1300°C. The structure of the steel was found to be as shown in Fig.3, i.e. completely devoid of eutectic. The ingot itself was perfectly sound, there being no trace of pipe or contraction cavities. It was also shown that this small ingot, when re-melted once more and cooled at a more normal rate, reverted to a typical eutectiferous cast structure. It appears to be proven, then, that elimination of eutectic in high-speed steel is possible if sufficient opportunity is given for equilibrium to be attained.

CONTROL OF THE PERITECTIC BY REHEATING

The method described in the previous paragraph, although feasible on the laboratory scale and perhaps in the production of small castings, may not be a practical method for treating comparatively large masses. The logical alternative therefore appeared to be to investigate the possibility of achieving a similar result by reheating the solidified ingot to the peritectic reaction temperature, to allow further reaction to proceed. Metallographic examination of as-cast high-speed steel ingot sections leads to the supposition that in severely cored structures there may be areas of carbon content up to 1·0% or higher: this deduction is based on the similarity between such structures and those found in banded ingots (Fig.2) in which the carbon segregation has been measured by analysis. According to Fig.1 it is apparent that in such higher-carbon areas the eutectic fusion temperature may be as low as 1260°C: this is confirmed by experience in industry. Therefore, on re-heating an as-cast alloy, it is to be expected that progressive fusion of eutectic can occur between 1260°C and 1345°C, the exact temperature depending on the local composition, and thus produce a condition similar to that which existed in the ingot when the temperature had fallen below the peritectic reaction temperature. Conditions should therefore be favourable for resumption of the peritectic reaction.

This was explored by experiment, and it was found that, by reheating even a banded specimen (Fig.2) to 1340°C for 30 min, complete elimination of eutectic took place, as shown in Fig.4. In view of the very severe carbon segregation, it is rather surprising that the reverse peritectic reaction has not persisted in parts of this specimen. This anomaly is discussed later in the paper. The inference seems to be that even the most severely segregated structures can be satisfactorily treated by this method which is being termed prespheroidizing and will be referred to as such in this



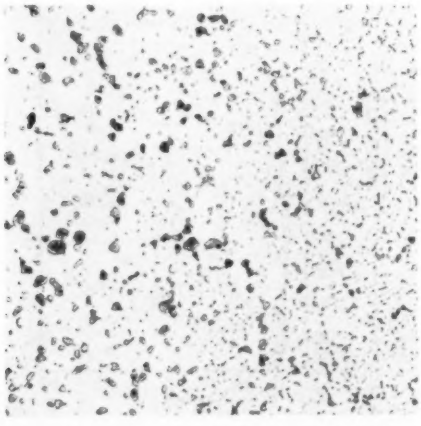
3 *Structure of small 18–4–1 steel slowly cooled from the melt* × 100

paper. A number of laboratory ingots responded satisfactorily to similar treatment.

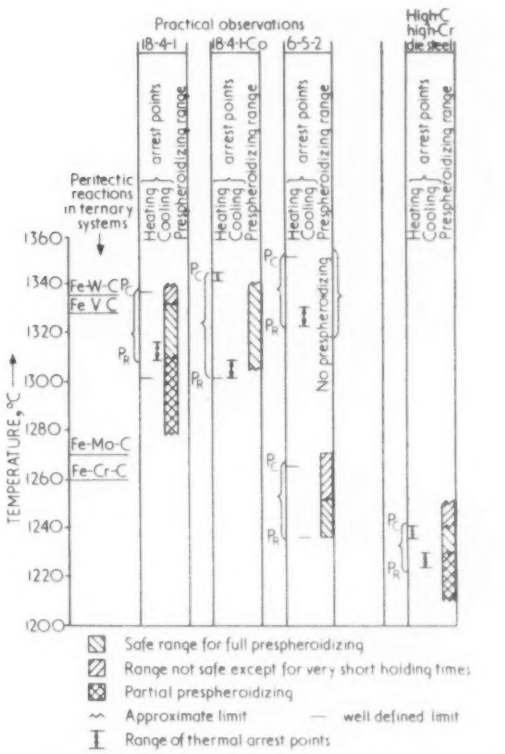
Since these experiments were made, it has come to the authors' notice that Russian workers[15] have made similar observations. They used induction heating and quote a temperature range of 1320–1380°C in which the eutectic structure in 18–4–1 high-speed steel can be changed, and suggest that this could be of importance in the treatment of cast tools.

APPLICATION OF PRESPHEROIDIZING IN PRACTICE

The experiments so far described show that the eutectic structure in high-speed steels can be eliminated by purely thermal methods. To exploit this new knowledge it was necessary to determine more precisely the conditions under which prespheroidization could be achieved for a typical range of commercial high-speed steels. In view of the apparent connection with the peritectic change points it seemed desirable to examine the thermal arrest points in relation to the structural changes taking place in that temperature range. Subsequently it was necessary to examine the effect of hot mechanical working on the structure of prespheroidized steels and the response of these steels to normal hardening and tempering.



4 *Structure of sample in Fig. 2 after ½ h at 1340°C* × 100



5 *Relationship between prespheroidizing temperature and thermal arrest points*

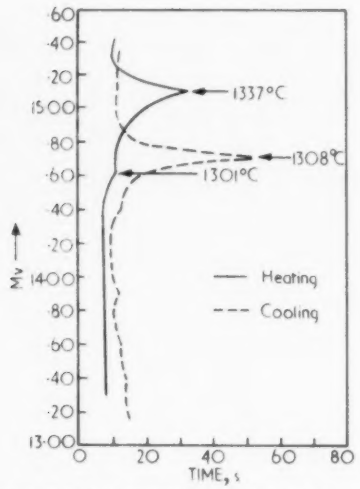
EXAMINATION OF THE PROBLEM BY THERMAL ANALYSIS

It has been stated that the original approach to the problem was based on the supposition that the peritectic reaction would, if allowed to proceed to completion, completely remove the mother-liquor from which eutectic could subsequently be formed. In view of the uncertainty regarding the range of peritectic temperatures in commercial high-speed steels, a series of experiments was undertaken to determine thermal arrest points for a number of samples from commercial ingots.

The technique used was conventional thermal analysis. Specimens were 1 in. × 0·75 in. × 0·4 in., with an axial hole drilled to accommodate a Pt/Pt–13%Rh thermocouple in circuit with a Carpenter-Stansfield potentiometer: the cold junction was maintained at 0°C. Heating and cooling were carried out in a manually controlled silicon carbide resistance furnace, and inverse-rate heating and cooling curves were plotted from readings of the time interval for successive 0·1 mV increments or decrements in thermocouple emf.

The results are summarized in Fig.5 but will be discussed in turn for each of the steels examined. In the absence of any recognized symbol for the peritectic reaction temperatures, the abbreviations P_c and P_R have been used in the following text to denote the temperatures of peritectic arrests found on heating and cooling respectively.

The steels investigated in this manner are listed in Table I and the observations made are as follows.



6 *Thermal analysis of as-cast 18–4–1 tool steel*

18-4-1 steel

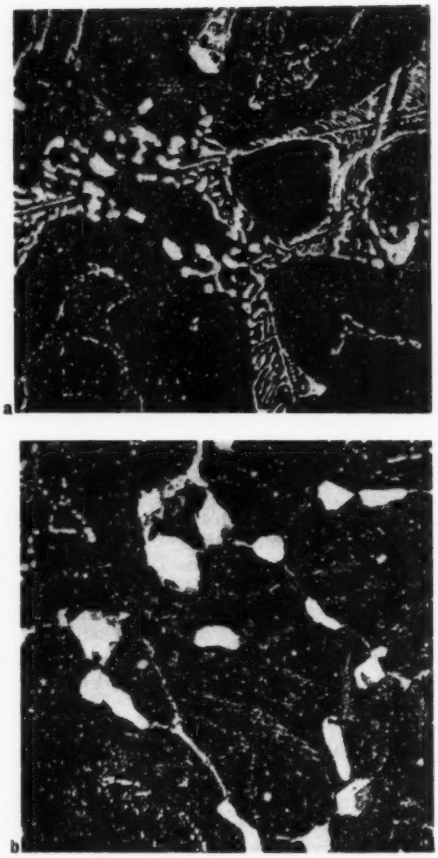
As-cast

A typical thermal analysis curve for this sample is shown in Fig.6. Arrest points on heating were found at 1301°C and 1337°C respectively, the latter being most marked. The main arrest point on cooling was found at 1308°C in two experiments and at 1316°C in a third. There was only inconclusive evidence on cooling of an arrest point corresponding to the 1301°C point in the heating curve. A similar observation was made at the NPL[11] and there remains doubt as to the precise significance of the arrest. The values (1337°C for P_c and 1308–1316°C for P_R) for the main arrest agree well with values of 1335°C and 1332°C quoted in the literature for the peritectic temperature in ternary Fe–W–C alloys.

Heating experiments confirmed that a half-hour treatment of this alloy at 1315°C eliminated the eutectic structure, and that a short treatment at 1360°C (above the arrest point) gave a eutectiferous structure. A further specimen was treated at 1285°C, i.e. below both arrest points, and was found to be partially prespheroidized (*see* below) as shown in Fig.7*a*. It is apparent from this micrograph that, whilst much of the eutectic retains its characteristic 'chinese script' structure, the outer extremities of the colony have become detached from the main carbide network with the formation of a series of small, mainly spherical carbide particles. Otherwise the structure retains most of the characteristics of an as-cast steel. Figure 7*b* shows, for comparison, the fully prespheroidized (1315°C) structure of the same steel at the same magnification. It is obvious that in the latter structure the carbide particles are larger, more angular, and bear no resemblance whatever to the eutectic from which they were formed. In the authors' opinion the two types of structure result from different mechanisms, the partial prespheroidizing effect being due to a purely physical phenomenon with no phase change.

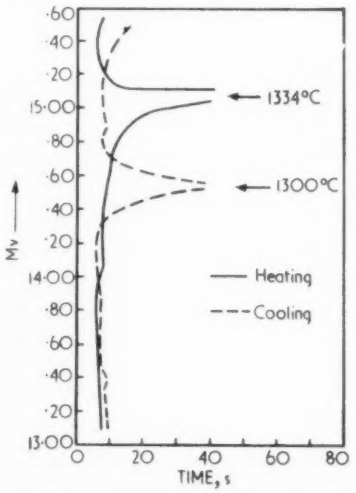
Bar stock

For comparison purposes, a sample of the same 18–4–1 bar stock used by the NPL[11] was subjected to thermal

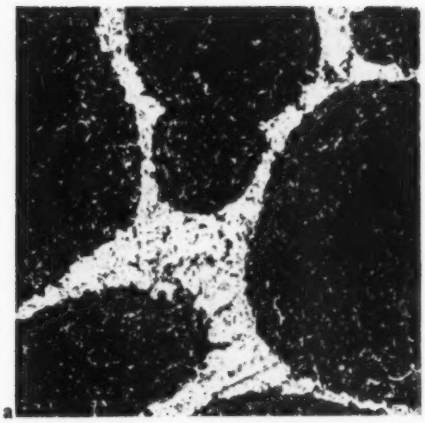


7 *As-cast 18–4–1 steel (a) treated ½ h at 1285°C, (b) treated ½ h 1315°C (Figs. 7, 9, 12, 14, etched 2% Nital)* × 500

analysis in the same way as the as-cast structure. Arrest points (Fig.8) were found at 1334°C (P_c) and 1300°C (P_R) respectively: these results agree fairly closely with those given by the NPL. Figure 9*a* shows



8 *Thermal analysis of 18–4–1 steel bar*



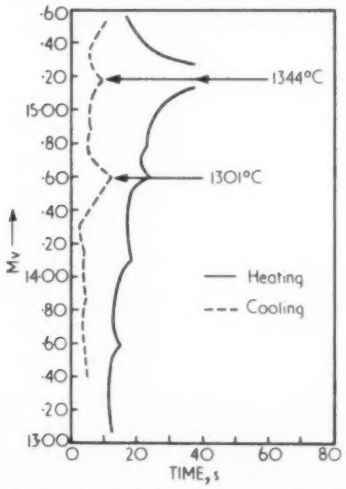
9 *18–4–1 tool steel bar (a) heated ½ h at 1360°C, (b) heated ½ h at 1360°C and ½ h at 1310°C, and (c) heated ½ h at 1305°C* × 500

the overheated structure of this steel, with its typical eutectic, and Fig.9*b* the result of overheating (1360°C) followed by prespheroidizing at 1310°C. This structure shows clearly that the carbide particles formed from the secondary eutectic occupy sites corresponding to the position of the eutectic, but otherwise the overheated structure has been eliminated. Figure 9*c* shows the structure of bar after heat-treatment at 1305°C (i.e. virtually a prespheroidizing operation) and there is little to distinguish this from a structure obtained by prespheroidizing a eutectiferous sample except, perhaps, that the individual carbide particles are not so large. The angularity of carbide is still pronounced.

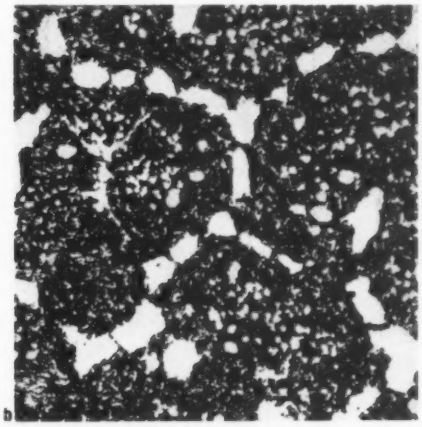
It is therefore apparent that similar structures can be obtained from bar material and from as-cast material by treatment in the region of the peritectic, and it must be concluded that this type of structure represents an equilibrium structure at that temperature.

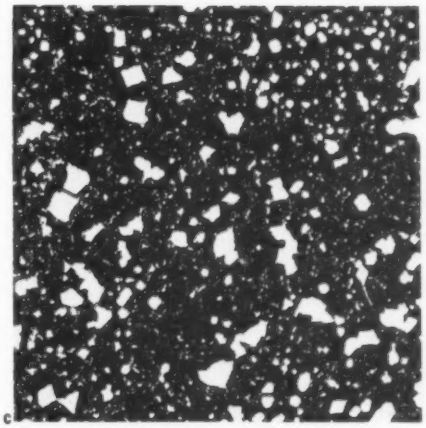
18–4–1 steel with 5%Co

A typical thermal analysis result is given in Fig.10. Arrest points on heating are observed at 1301°C,



10 *Thermal analysis of 18–4–1 + 5%Co tool steel*



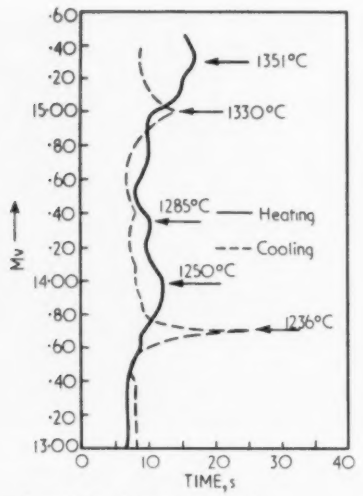


1344°C and, in another determination (not shown) at 1416°C. Arrests on cooling are at 1344°C (probably corresponding with the heating arrest at 1416°C), and at 1301°C. The latter arrest occurred at 1308°C in another experiment. It thus appears that the peritectic arrests are at 1344°C and 1308°C on heating and cooling respectively. The effect of cobalt would seem to be to increase the sluggishness of phase changes. Otherwise the temperature of the phase change does not appear to be greatly affected by the presence of cobalt.

Structural changes correspond with the arrest points in the same way as for 18–4–1, the effect of the increased difference between heating and cooling transformation points being accompanied by an increase in the permissible prespheroidizing range.

6–5–2 steel

Thermal analysis results for 6–5–2 steel are given in Fig.11. The system is obviously less simple than for the tungsten and tungsten–cobalt types. The heating curve in particular is not clearly defined, but there appears to be a number of inflections that may be related to genuine phase changes, the most pronounced of which are at 1250°C and 1351°C with a doubtful point about 1285°C. Another experiment gave a sharper arrest at 1265°C. On cooling, clear arrests are apparent at 1330°C (1330°C and 1323°C in two other experiments) and at 1236°C (constant in three tests).



11 *Thermal analysis of 6–5–2 tool steel*

It appears that the molybdenum peritectic reaction is taking place between 1236°C and about 1250°C, and that the higher points (1351°C and 1330°C) are due to the same peritectic reaction that occurs in 18–4–1 steels at similar temperatures.

The inference is that in the more complex alloy the individual peritectic reactions can still take place as they do in ternary systems, at least under the conditions prevailing in as-cast commercial materials. A similar observation has been made in 18–4–1 steel, in which a constituent believed to be vanadium eutectic appeared at temperatures just below those required for full reversion to the normal tungsten eutectic. This agrees with Kuo's[13] observations concerning the peritectic temperature in the Fe–V–C and Fe–W–C ternary systems, although no arrest has been observed that could be attributed to the vanadium reaction: such an arrest would probably be masked by the adjacent major arrest attributed to the tungsten reaction.

The effect of heating an as-cast sample of this steel for 30 min at 1230°C is shown in Fig.12*a*. It is apparent that a very slight thickening of carbide has taken place with no real tendency to prespheroidize. Figure 12*b* shows the effect of a similar treatment with an additional 1-min treatment at 1270°C before quenching: here a partially prespheroidized structure is obtained. Treatment for 30 min at 1240°C resulted in more complete prespheroidizing as shown in Fig.12*c*, and an extra 5-min treatment at 1270°C gave the eutectiferous structure shown in Fig.12*d*. These eutectic formations were of rather different appearance from those normally found in molybdenum high-speed steels and were more reminiscent of tungsten eutectic. Heating to 1305–1312°C (Fig.12*e*) and to 1416°C (Fig.12*f*) both caused reversion to a normal 'molybdenum' eutectic formation, but in Fig.12*e* there is evidence of two types of eutectic in juxtaposition, indicative of some transition from one to the other.

High-carbon high-chromium die steel

The results of thermal analysis are shown in Fig.13, from which it appears that a phase change occurs on heating and cooling at 1240°C and 1229°C respectively (1235°C and 1223°C in another determination). It is not certain that this arrest represents a peritectic reaction.

The as-cast structure is shown in Fig.14*a* and Figs.14*b–d* respectively illustrate the effects of 30-min treatments at 1230°C, 1240°C, and 1250°C followed by quenching and annealing.

The full prespheroidizing reaction appears to have taken place at 1240°C (Fig.14*c*) but partial effects are observed after treatment at 1210°C or higher. A 1250°C treatment (Fig.14*d*) results in reversion to eutectic.

EXPERIMENTAL DETERMINATION OF CONDITIONS FOR PRESPHEROIDIZING

From the foregoing thermal analysis there is good evidence that the structural changes occurring during prespheroidization are primarily associated with the peritectic reaction, although there may be a secondary effect due to physical agglomeration of limbs of the eutectic phase. For practical purposes, however, it was desired to establish the effects of time and temperature on the progress of the changes.

For the purpose of this exploration a conventional electrically heated high-temperature furnace, designed for tool heat-treatment, was employed. Control of temperatures was maintained within ± 5°C in all experiments. The samples for examination were sections 1 in. × $\frac{3}{4}$ in. × 2 in. cut from slices of 4-in. square ingot kindly supplied by tool steel manufacturers. Prespheroidizing treatments were applied for $\frac{1}{2}$, 2, and 4 h at different temperatures chosen in the region of the peritectic arrest points determined for each type of steel examined. The results of these trials are given in Table II.

In compiling this table the structures obtained have been divided into categories as follows, according to the degree of change of structure:

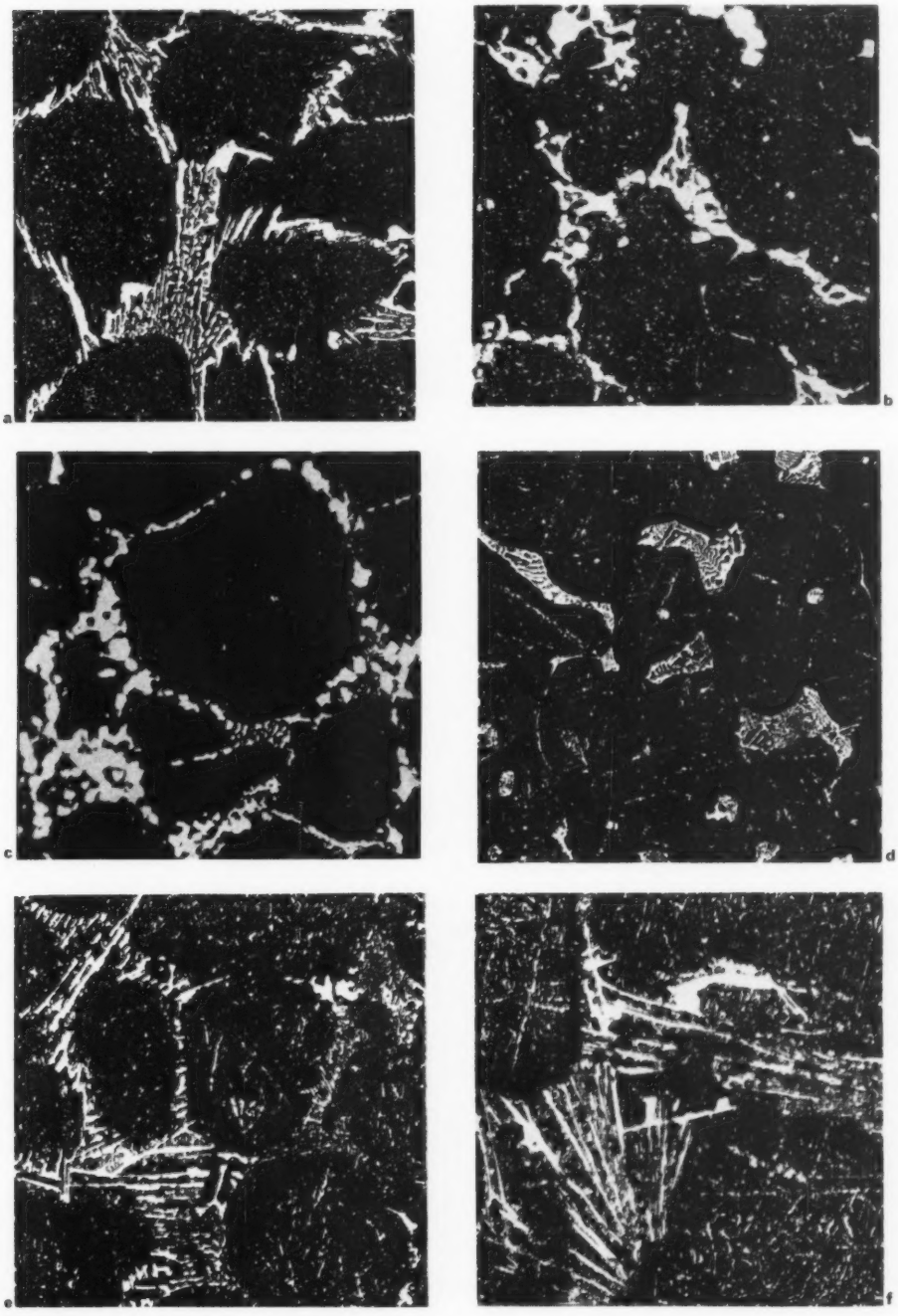
- *X*: eutectic still present in original cellular form, but in certain cases partial disruption of carbide may have occurred. This structure does not contain cubic-type carbides, typical of the fully prespheroidized material, and is referred to as 'partially prespheroidized' (Fig.7*a* is typical)
- *A*: most of the primary eutectic has been eliminated, and small angular carbide particles formed in its place, but the original coring effect is still well defined, as marked by delta eutectoid boundaries (Fig.15*a* is typical)
- *B*: eutectic completely absent, small or medium angular carbide particles present, but traces of original coring present in the form of delta eutectoid colonies (Fig.15*b* is typical)
- *C*: fully prespheroidized, with complete absence of eutectic and of delta eutectoid, the carbides being almost entirely small angular or rounded particles distributed at random (Fig.15*c* is typical)
- *D*: no trace of original structure, but carbide particles tend to be enlarged and more angular than in stage *C*, with some evidence of secondary eutectic in small colonies (Fig.15*d* is typical)
- *E*: typical overheated structure, with complete secondary eutectic network and cored eutectic-eutectoid configurations (Fig.15*e* is typical)

In general, all structures observed can be classified in one of the above categories, but in certain alloys not all stages have been observed. The treatment of various alloys will now be considered in turn as follows.

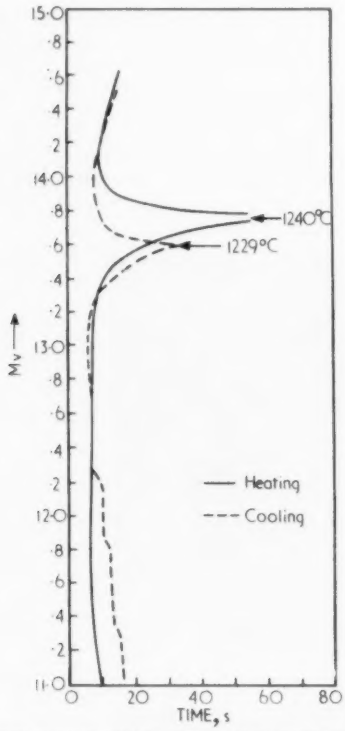
18–4–1 steels

From the data in Table II it is apparent that the critical temperature range for prespheroidizing is 1310–1330°C. It would appear from the results that the steels with lower carbon contents (562 and 563) are most susceptible to damage by excessive heating. As would be expected, the effects of time and temperature are interdependent and longer holding times are permissible at the lower end of the range. The upper temperature limit (1330°C) must therefore be regarded as an absolute maximum and not a safe treatment temperature for prespheroidizing. In stage *D*, the secondary eutectic was believed (from comparison with Kuo's illustrations) to be vanadium-rich.

It is worthy of note that the overheated structure (stage *E*) is generally characterized by smaller and finer eutectic colonies than are as-cast structures. This suggests that an as-cast and overheated structure



12 *6–5–2 steel (a) heated ½ h at 1230°C, (b) as in (a) plus 1 min at 1270°C, (c) heated ½ h at 1240°C, (d) as in (c) plus 5 min at 1270°C, (e) heated ½ h at 1305–1312°C, and (f) heated ½ h at 1416°C* × 500



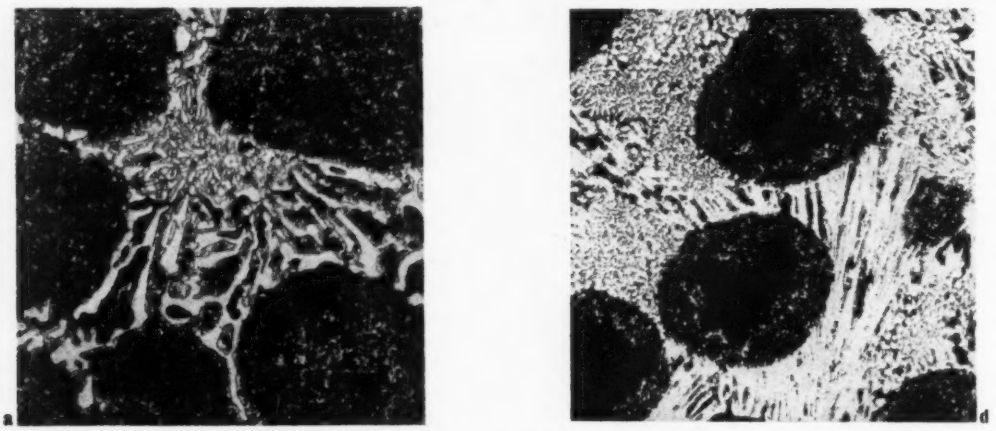
13 *Thermal analysis of high-carbon high-chromium die steel*

might give a more uniform carbide distribution on forging than an untreated ingot. The eutectic-free structure is, however, considered to be preferable.

It has been mentioned earlier that an overheated bar structure in an 18–4–1 steel can be prespheroidized in exactly the same way as can an as-cast structure, as shown in Figs.9*a* and *b*. Thus it is established that the eutectic structures may be produced or eliminated in this type of steel, more or less at will, and it may confidently be deduced that this reversible change is in fact brought about by a phase transformation.

18–4–1 steels with cobalt

Three steels in this classification were examined, two with 5% and one with 10%Co. It is apparent from the results that prespheroidizing of these steels can be achieved at a wider range of temperatures than can 18–4–1 steel: there is, in general, less risk of obtaining class *E* structures and at the same time a prespheroidized structure can be obtained at temperatures as low as 1280°C, although optimum results are obtained in the region of 1310°C. Structures are very similar to those shown for 18–4–1 steels and are not reproduced.



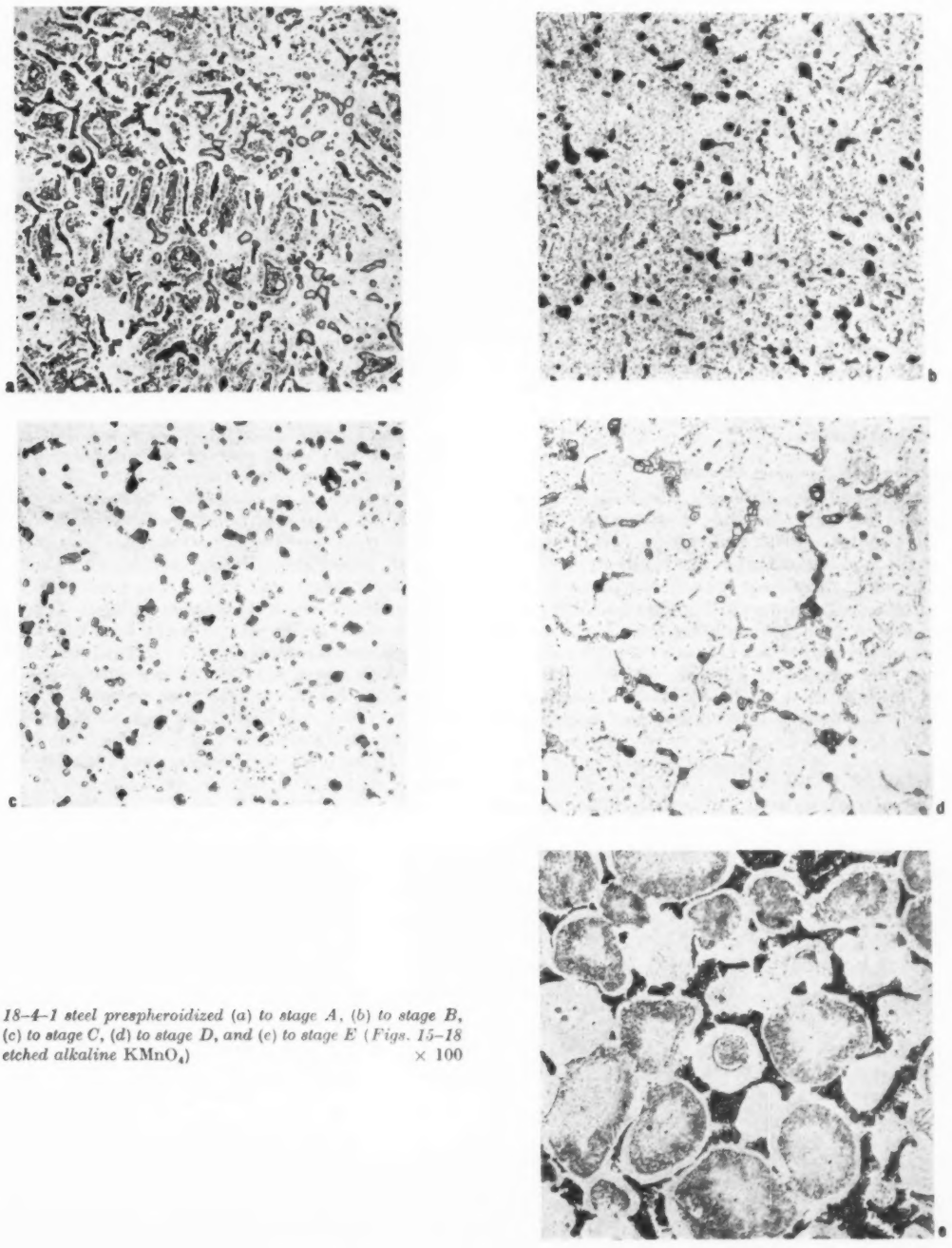
14 *High-carbon high-chromium die steel* (*a*) *as-cast structure,*(*b*) *heated* ½ *h at 1230°C* (*c*) *heated* ½ *h at 1240°C, and* (*d*) *heated* ½ *h at 1250°C* × 500

Molybdenum high-speed steel (6–5–2)

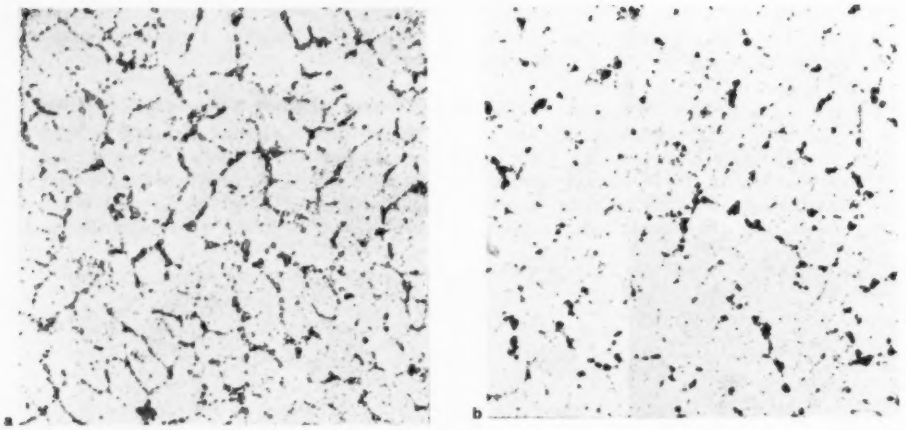
The structures obtained from this type of steel are slightly different from those described for 18–4–1 types. The as-cast structure is generally similar to that shown in Fig.17*a* for a more complex steel. Stages *B* and *D* have not been encountered with these steels, and Figs.16*a* and *b* show the structure corresponding with stages *A* and *C* respectively. As would be expected from the known hardening and overheating characteristics of these steels, the prespheroidizing range is substantially lower than for tungsten types, namely 1250–1270°C with a probably optimum temperature of 1260°C.

Tungsten-molybdenum-vanadium-cobalt steels (high carbon)

The as-cast structure of this steel is shown in Fig.17*a* and is similar in character to that of a typical 6–5–2 steel. The prespheroidizing range (Table II) is seen to be of the same order as that given for molybdenum types, but covers a wider range of temperatures. It



15 *18–4–1 steel prespheroidized* (a) *to stage A*, (*b*) *to stage B*, (c) *to stage C*, (d) *to stage D, and* (e) *to stage E* (*Figs. 15–18 etched alkaline* $KMnO_4$) × 100



16 *6–5–2 steel prespheroidized (a) to stage A, and (b) to stage C* × 100

thus appears that the dominant element governing the reaction is molybdenum, but that cobalt has a similar effect to that reported for 18–4–1 steels, i.e. in increasing the permissible prespheroidizing range.

The structure obtained by prespheroidizing for ½ h at 1250°C (stage *C*) is shown in Fig.17*b*. Clearly two types of carbide are present, the dark etching M_6C and the unattacked *M*C (presumably VC) particles. During the investigation it was observed that the secondary eutectic found in type *D* structures was M_6C eutectic, and that the more uniformly dispersed VC particles remained unaffected.

High-carbon high-chromium die steel

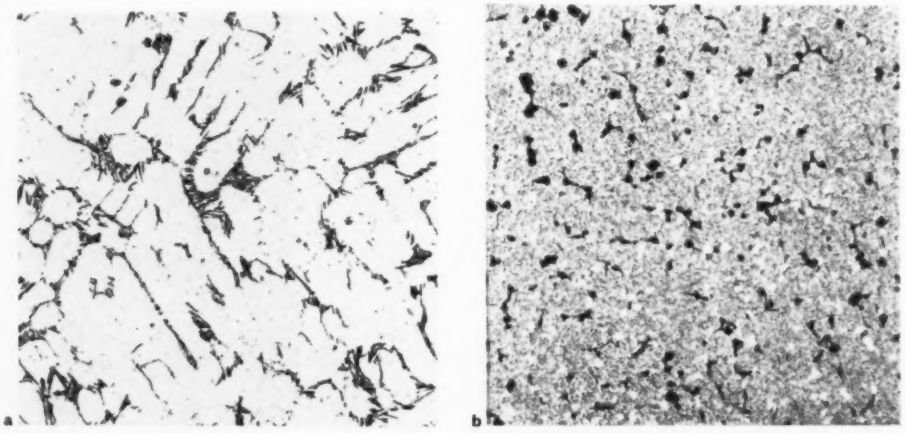
As mentioned earlier, although this die steel is not a high-speed steel, its structure is not vastly different and the two types both suffer from carbide heterogeneity. It was found that a sample of this steel (TS 537) responded satisfactorily to treatment at 1230°C, the structure being shown in Figs.18*a* and *b*. It would appear that a prolonged holding time has a greater effect on grain growth for this steel than for 18–4–1, although the sample of Fig.18*b* was taken from a coarser grain portion of the ingot. At 1250°C a reversion to grain boundary eutectic is observed (Fig.18*c*).

DISCUSSION OF RESULTS

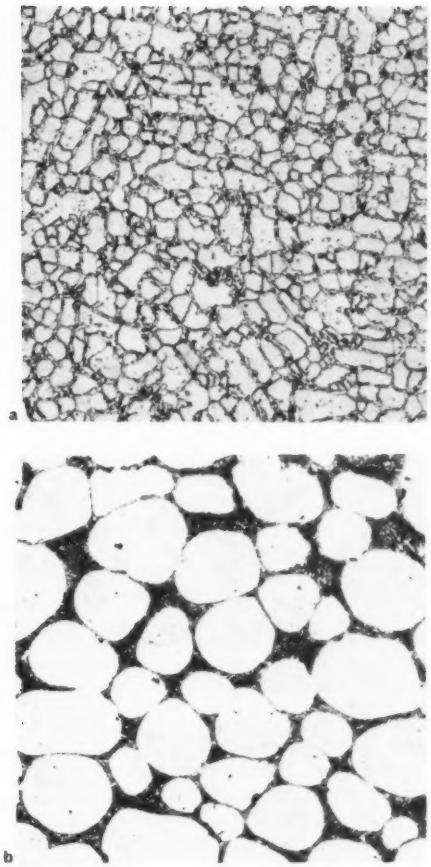
The thermal analysis results indicate conclusively that the process of prespheroidizing is closely linked with the peritectic reaction. There are, however, certain anomalous results which, at first sight, seem to contradict this theory. Firstly, it is found that in certain cases short-time treatments give prespheroidized structures, whereas longer treatments at the same temperatures give reversion to eutectic. Secondly, it has been shown that high-carbon areas (1·0%C) in 18–4–1 steels can be prespheroidized, whereas the available constitution diagram suggests that the peritectic reaction should, in this case, proceed in the reverse direction. Thirdly, there is the fact that partial prespheroidization can occur at temperatures substantially below the peritectic temperature.

To explain these anomalies it is necessary to examine in detail the relationship between the peritectic temperature range and the structural changes which take place at temperatures below, within, and above this range, for a number of steels. A summary of the experimental observations is made in Fig.5. The ranges of peritectic reaction have already been discussed for the steels examined, and in all cases it is found that the range of full prespheroidizing lies within the limits marked by the P_c and P_R temperatures. In certain cases (e.g. the 6–5–2 steel) a very short treatment above P_c can result in prespheroidizing, but only between P_c and P_R can longer treatments be safely conducted.

In the case of 18–4–1 and 12%Cr steels the phe-



17 *W–Mo–V–Co steel (a) as-cast, and (b) prespheroidized to stage C* × 100



18 *High-carbon high-chromium die steel (a) fine grain, prespheroidized 1230°C, (b) coarse grain, prespheroidized 1230°C, and (c) heated ½ h 1250°C* ×100

nomenon of partial prespheroidization takes place below P_R, and at temperatures slightly above P_R. In view of the structural difference between partially and fully prespheroidized structures, it is concluded that the two effects are due to different phenomena: the full effect (giving angular particles) being due to the peritectic reaction but the partial effect being a purely physical phenomena.

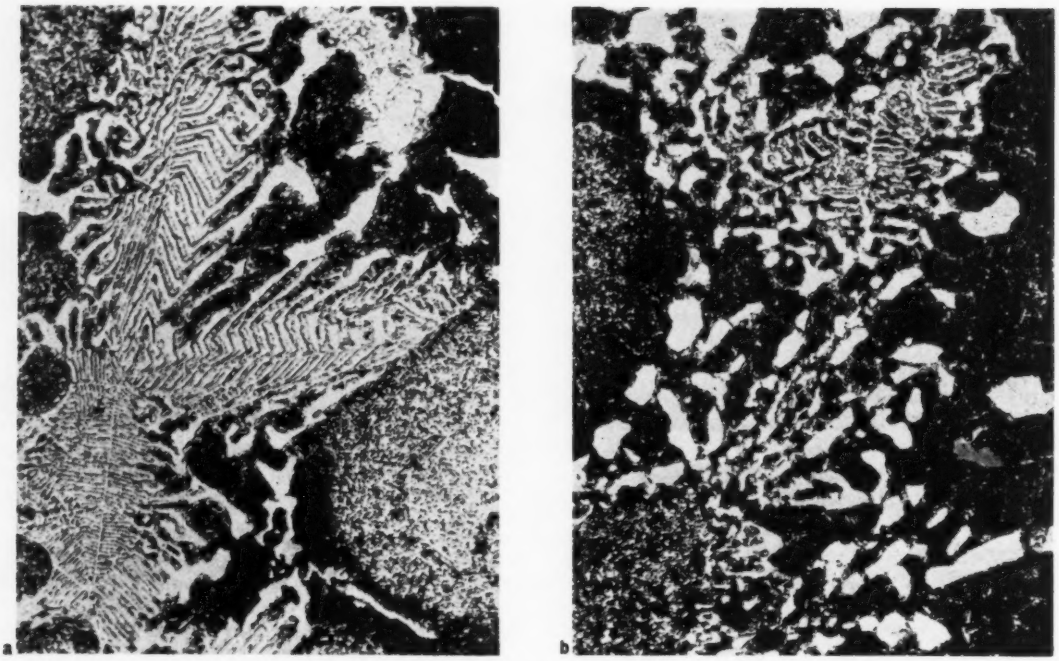
This view is supported by metallographic observation of eutectic in 18–4–1 samples heat-treated at 1250°C (Fig.19*a*) and 1300°C (Fig.19*b*). Figure 19*a* shows that the extreme ends of the eutectic branches are becoming thickened and in some cases particles have become separated from the main colony. Also, in two or three places, carbide from adjacent branches has become joined, to form a closed ring of carbide. Such carbide formations are, in the authors' opinion, the origin of the so-called 'bird's eye' carbide formation which is sometimes taken as evidence of incipient overheating in tool steel samples. It is now suggested that this phenomenon, when observed in normal bar stock, is probably due to the first stage of eutectic break-up having occurred during heating for forging. Figure 19*b* shows a more advanced stage with full spheroidization of eutectic branches, and some 'bird's eye'.

It is apparent that the length of treatment only becomes important at temperatures near to P_c and it is suggested that the actual limit of safe prespheroidizing might be marked by the equilibrium value of the peritectic temperature. It is known that reactions are somewhat sluggish in high-speed steels, especially those containing cobalt, and this might well account for the fact that some treatments are possible above P_c. This may be an explanation of the fact that Russian work[15] gives higher temperature limits.

A further point to be considered is that in the more complex alloys the peritectic reaction is probably quaternary, or even of higher order, and will be marked by a range of temperature. It is, for example, noted that the range P_c–P_R is the narrowest (roughly 15°C) for high-carbon high-chromium die steel, which is essentially a ternary Fe–Cr–C alloy. This temperature difference appears to represent a normal thermal lag between heating and cooling arrest points, and it seems reasonable to expect a similar lag for other alloys. If this is so, then an approximation can be made concerning the temperature range of the peritectic reaction. For 18–4–1, P_c–P_R is about 25°C, so that a peritectic range of the order of 10°C is obtained. This seems to agree with Murakami and Hatta's data. In the same way, peritectic ranges of 25°C and 10°C are deduced for the 18–4–1 cobalt steel and the 6–5–2 steel. The 12%Cr die steel is, of course, presumed to have an almost invariant peritectic temperature. In general, these deductions are compatible with the tolerances in the prespheroidizing operation.

According to the constitution diagram (Fig.1) for 18–4–1 steels, it was predicted that alloys of carbon content greater than about 0·7% should solidify with eutectic formation. On the same grounds one would expect holding such an alloy at peritectic temperatures to yield eutectic and not a prespheroidized structure. That this is not the case is apparent from the behaviour of the high-carbon zone in the sample whose structure is shown in Fig.2.

It may not be too difficult to explain the anomaly whilst still adhering to the peritectic theory. In Fig.1 the phase boundary lines are drawn for systems in equilibrium or, more correctly, for systems in a state as near to equilibrium as could be attained experimentally. It is reasonable to suppose that the as-cast structures under consideration in this paper are in fact far from equilibrium and it is inferred that a modified metastable condition may exist during the early stages



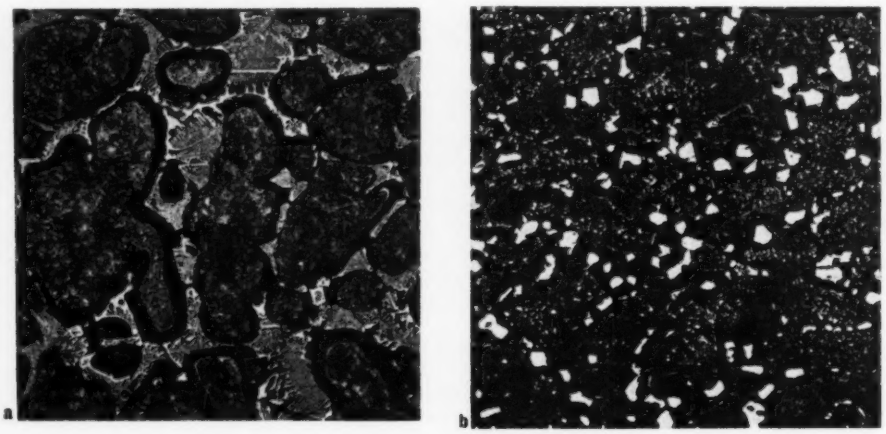
19 *Eutectic in 18–4–1 steel (a) heated at 1250°C and (b) heated at 1300°C (Figs. 19–22 etched 2% Nital)* × 500

of prespheroidizing operations. On reheating to prespheroidizing temperature it is fairly certain that some of the eutectic present will melt, and the phases present will be austenite, liquid, eta-carbide, and possibly some untransformed ferrite. In order to attain equilibrium between the phases, time is obviously necessary, especially in view of the high concentration of carbide to be taken into solution. It may be, therefore, that the initial liquid is not so high in carbon as was the eutectic from which it was formed and is of composition favourable for the peritectic reaction to consume the liquid phase. The same argument would predict that ultimately carbide solution should proceed to completion, causing a reversal of the peritectic and formation of new eutectic. Under these circumstances an optimum holding time should be found to give the most beneficial results.

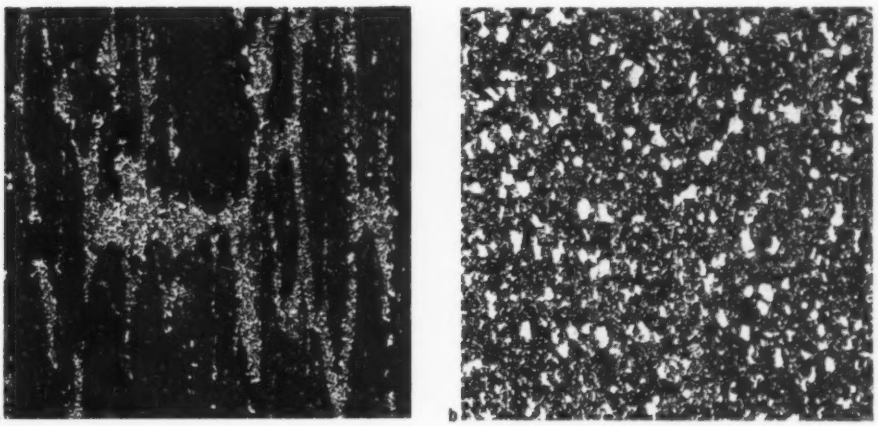
In discussing this problem it must be borne in mind that little or no information exists concerning local alloy segregations, which may have a considerable effect on the equilibrium system shown in Fig.1. It is suspected that in the areas immediately surrounding eutectic colonies the alloy content might be quite high but there is no indication of which alloys are involved or the extent of the segregation. It is hoped that techniques such as the micro-beam method of analysis developed by Castaing[16,17] and described by Philibert and Crussard[18] might be of assistance in this problem.

COMMERCIAL APPLICATION OF PRESPHEROIDIZING

Although it has been conclusively demonstrated that under laboratory control the prespheroidizing treatment is capable of eliminating the eutectic phase from cast high-speed steel, commercial application of the



20 *18–4–1 steel (a) as-cast, and (b) prespheroidized* ×100



21 *1¾-in. billet from (a) as-cast 18–4–1 steel ingot, and (b) prespheroidized 18–4–1 steel ingot* × 100 (longitudinal section)

process to larger masses presents some difficulties.

In the first place it is well known that as the size of ingots increases, so the carbide eutectic formations become more coarse and hence may be more difficult to disperse. Experiments are in hand to examine the effect of the prespheroidizing treatment on sections from ingots up to 12-in. square and it may be necessary to extend the work to even larger sections. Preliminary results on 8-in. square ingots have, however, been very promising. Should serious difficulties be experienced with really gross eutectic colonies it may still be desirable to attempt some carbide refinement during the melting and casting stages, perhaps as previously illustrated by the authors.[3]

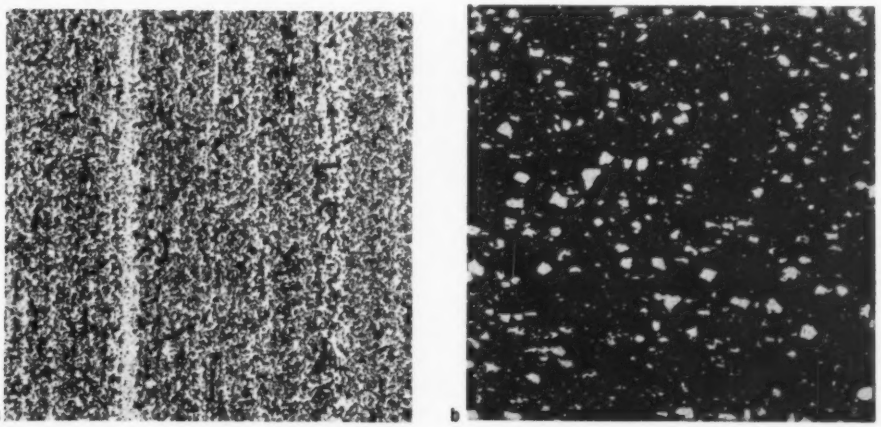
Further, the full effects of the wide variations of composition possible in high-speed steels are not known with any certainty and some discrepancies may come to light which will require further exploration before a complete understanding of the process and its requirements is achieved.

The permissible temperature ranges over which prespheroidizing has been found to be successful are strictly limited and this will pose considerable problems with existing furnace installations, which were not designed for such refined treatments. Also, serious decarburization and loss of molybdenum is likely to take place. Hence it is likely that some adaptation of furnace capacity will be necessary to make use of the treatment on a worthwhile scale.

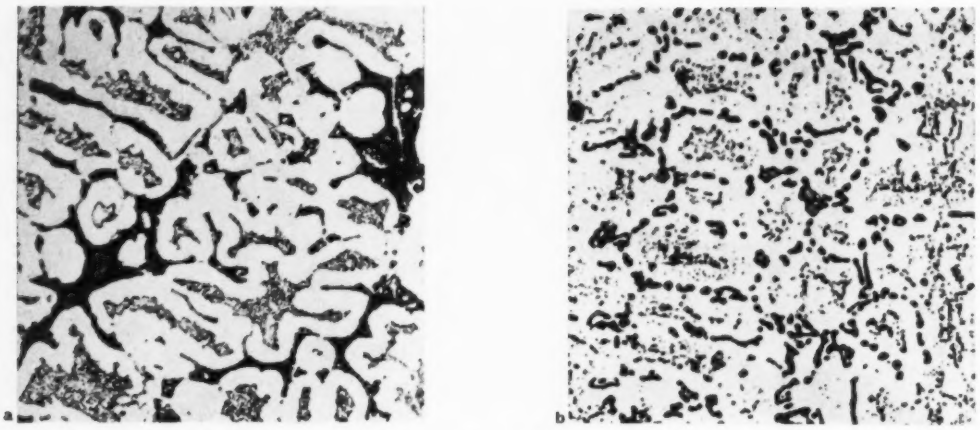
It has been possible to treat a limited number of 3-in. hot extrusion slugs, and very satisfactory extrusions have been produced from this material, as described by Sukolski and Hoyle.[19] On a larger scale attempts have been made to treat complete 4-in. square ingots, using a somewhat unsuitable furnace. Here, although thermocouples were embedded in the ingots, it was not always possible to avoid partial ruination of the ingot due to local overheating. Despite this, some parts of ingots were successfully treated and to demonstrate the effect of hot work, these were forged and rolled. Figures 20*a* and *b* show the structure of a normal and a prespheroidized 4 in. square ingot of high-speed steel: Figures 21*a* and *b* and the 1¾ in. square billets forged from these and Figs. 22*a* and *b* the rolled ½-in. dia. bar. The elimination of stringers from the prespheroidized samples is strikingly apparent. The bars were, however, decarburized to a considerable depth during the prespheroidizing operation.

Rectification of overheated structures

It has already been demonstrated that overheated bar samples of high-speed steel can be prespheroidized in the same way as can as-cast structures. This offers the possibility of re-treating tools that may have been accidentally overheated in heat treatment. Admittedly this may be a minor consideration for drills, lathe tools, etc., but in the case of expensive tools a



22 *½-in. dia. bar from (a) as-cast 18–4–1 steel ingot, and (b) prespheroidized 18–4–1 steel ingot* × 100 (longitudinal section)



23 (*a*) *Structure of cast 18–4–1 tool steel,* (*b*) *as* (*a*) *after prespheroidizing* × 500

considerable saving could result by avoiding the scrapping of a single piece.

Application to cast cutters

There is a limited field of application for cast high-speed steel cutters in the UK, and the prespheroidizing process offers the possibility of improving their structure by removing the eutectic. Figures 23*a* and *b* show the structures of a typical cast tool before and after prespheroidizing. In general, the structure of cast cutters is finer than normal ingot structures, so that the prespheroidizing operation gives comparatively uniform carbide distribution.

HARDENING AND TEMPERING CHARACTERISTICS OF PRESPHEROIDIZED HSS BAR

A survey of the response of prespheroidized material (18–4–1) to heat-treatment has been undertaken. Particular attention was paid in this case to an observation of the upper maximum temperature permissible for the austenitizing treatment. It was thought possible that the previous high-temperature treatment might have raised the temperature of incipient eutectic formation in the ultimate product, but this was not found to be the case and the heat-treatment range was essentially the same as that for normal high-speed steel bar of similar composition, samples of which were treated side by side with prespheroidized material for comparison purposes.

In general the grain size of hardened prespheroidized samples has been quite satisfactory, but although one or two samples showed a larger grain size than normal, this was not excessive. The martensitic structure of prespheroidized samples was more pronounced, suggesting that a greater degree of austenite breakdown occurred in quenching, possibly as a result of greater matrix homogeneity. The hardening–tempering characteristics are given in Figs.24 and 25. It is apparent that the as-quenched hardness of prespheroidized material is higher than that for the comparison sample, but that on secondary hardening the position is reversed, which appears to confirm the metallographic observation of more complete austenite breakdown on quenching a prespheroidized material. The steady rise in final (double tempered) hardness of prespheroidized samples with austenitizing temperature indicates that this material is, as would be inferred from Niesielski's observations on conventionally worked material, more resistant to carbide solution and that a slightly extended austenitizing time would have beneficial effects when the lower temperatures were employed. The tempering characteristics of prespheroidized material follow the normal pattern, peak secondary hardness being attained at 550–560°C.

In general, with normal hardening and tempering practice prespheroidized high-speed steel appears to attain a satisfactory hardness, of the order of 830–860 DPN, which is considered to be generally acceptable. The sample used for comparison in the present instance is considered to have given unusually high hardness readings.

SUMMARY AND CONCLUSIONS

The eutectic structure of high-speed steel normally observed in the as-cast state can be eliminated by a simple high-temperature treatment which has been called prespheroidizing.

The temperature range in which this reaction takes place appears to be similar to the range in which the peritectic reaction can proceed.

Structures produced by prespheroidizing cast high-speed steels are very little different from those produced by heating wrought bar stock in a similar manner.

The prespheroidizing process can be reversed by raising the operating temperature, when reversion to eutectic occurs. The reverted structure can be retreated to remove the eutectic.

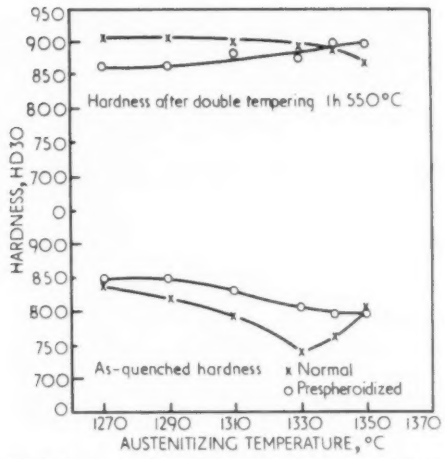
When present, molybdenum seems to have a dominant effect on the temperature of prespheroidization.

Cobalt has little effect on operating temperature.

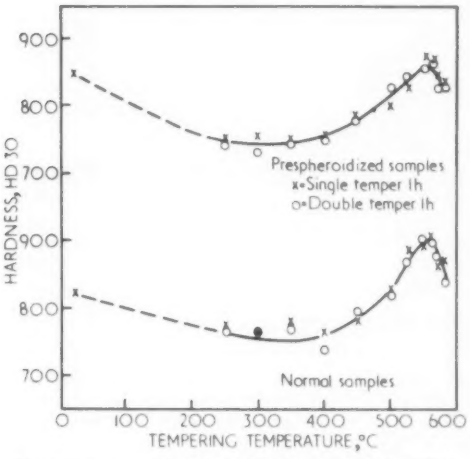
Bars produced from prespheroidized ingots are substantially devoid of carbide stringers, respond satisfactorily to conventional hardening techniques and are, in general, more homogenous than un-treated material.

The problems involved in treating large masses are considerable, and further development work is necessary to establish the treatment as a satisfactory commercial process.

For smaller sections such as cast cutters and hot extrusion slugs the treatment could be applied with very little further development.



24 *Effect of quenching temperature on hardness of high-speed steel*



25 *Tempering curves for 18–4–1 steel austenitized 1290°C*

In some circumstances the treatment may have some value in restoring overheated structures in high-speed steels.

ACKNOWLEDGMENTS

The authors are indebted to members of the BISRA Tool Steels Committee for helpful advice and encouragement, and also for the provision of samples. Much of the experimental work has been undertaken by Mr D. B. Hammond, L.I.M., Mr A. Hollingsworth, L.I.M., and Mr C. Binney, to each of whom the authors are deeply grateful for the skill and attention they devoted to the work.

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News of Special Reports

Two new volumes have recently been published in the Institute's Special Report Series, and may be obtained on application to the Secretary.

Precipitation Processes in Steel is the title of no.64, which is a full report of a meeting held in Sheffield in July 1958, and organized by the Department of Metallurgy of Sheffield University. Sixteen papers by leading workers in this field, from Belgium, France, Germany, Sweden, the UK, and the USA were presented. They were divided into four groups, covering precipitation in ferritic steels, precipitation in austenitic steels, intergranular fracture in steels, and precipitation during creep. These papers are printed in full in the report, together with the lively and informative discussions at the meeting. There is little doubt that this is the most valuable book published in recent years on precipitation processes in alloy steels, a vitally important subject today, when alloy steels are required for a great range of new applications.

Special Report no.65 is entitled *Handling and Treatment of Iron Ores*, and is a report of the meeting of the Institute's Iron and Steel Engineers Group held in Amsterdam in April 1959. Following an authoritative survey of the world iron-ore situation by Mr C. R. Wheeler, C.B.E., there are a number of papers dealing with various aspects of the subject. The installations at the Port of Rotterdam and IJmuiden in Holland, at Contrecoeur and Seven Islands, Canada, and at Margam and Cardiff in South Wales are described in a series of papers. There is an excellent survey of unloading facilities in the USA and Canada by Dr H. R. Mills of BISRA, and Mr T. V. Nicholson of the BTC contributes an account of the rail transportation of iron ores in the UK. In addition, there are papers on crushing, screening, handling, and blending plant for iron ores.

Report no.64 costs £4 4*s* 0*d* (£3 3*s* 0*d* to members of the Institute) and report no.65 costs £3 5*s* 0*d* both to members and non-members. Postage is 1*s*.

The extrusion of high-speed steel sections

P. J. Sukolski, B. E.(Hons.), and G. Hoyle, B.Sc., A.I.M

SYNOPSIS

Experiment has shown that ingots of 18–4–1 high-speed steel can be extruded to round or flat bars at temperatures ranging from 1150° to 1280°C, with extrusion ratios up to at least 54 : 1. Extrusion from temperatures between 1250° and 1280°C appeared to promote improved carbide distribution, particularly when an extended heating time was used. Satisfactory bars were obtained, even from unsound ingots, both statically and continuously cast, provided oxidation was avoided in heating. The properties of bars were found to be comparable with those of forged and rolled bars, either after conventional hardening or after cooling direct from the extrusion and tempering.

1753

INTRODUCTION

IN NON-FERROUS METALLURGY extrusion has long been accepted as a major production process, but its adoption in ferrous industry has been slow. Although stainless-steel tubes have been extruded in the UK for some 30 years,[1] it was not until the introduction of the Ugine–Séjournet process[2*] in 1945 that the method became commercially feasible for a wider range of products. According to this process glass is used as a lubricant and also acts as a thermal insulator, protecting the tool materials from the hot billet.

As yet, the economics of converting steel ingots into bars by extrusion are such that the process is not attractive for many applications. However, it is particularly attractive where batch sizes are small, where rapid changes are required from one section to another, where sections cannot be produced by rolling and where materials are difficult to hot-work. It is with the last of these that the present paper is concerned.

General observations have indicated that tool steels are at present shaped by a rather long and sometimes very costly sequence of operations. High-speed steels, for example, are often forged as well as rolled, in combination with several reheats, and finally the bars are machined, sometimes very extensively, to bring them to their final shape. It appeared that if extrusion could replace the present sequence of processes, the whole shaping could be done in a single operation with a single reheat and finally with considerably less machining.

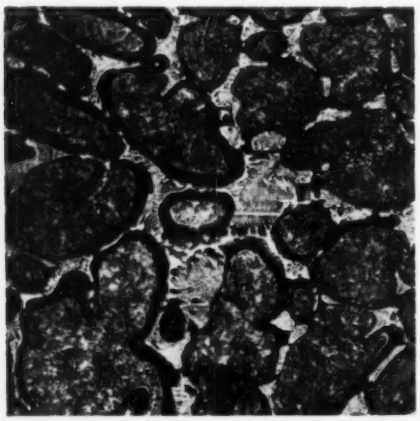
In the investigation described in this paper an 18–4–1 high-speed steel was chosen as the work material. It became apparent not only that it was practicable to extrude this material under a variety of conditions, but that a product at least equal to that obtained by forging and rolling could be obtained.

The as-cast structure of a typical high-speed steel of the 18–4–1 type (general composition 18%W–

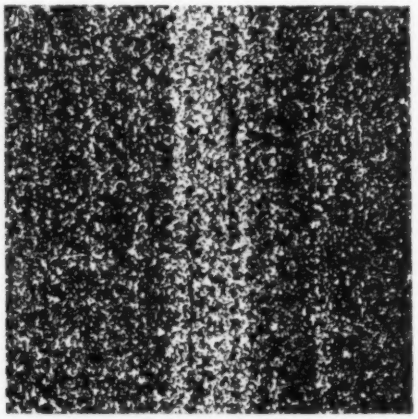
Paper MW/G/35/59 of the Extrusion Committee of the Mechanical Working Division of the British Iron and Steel Research Association, received 6 August 1959. The views expressed are the authors' and are not necessarily endorsed by the Committee as a body.

Mr Sukolski was formerly on the staff of the Mechanical Working Division and is now with McKechnie Bros Ltd, Aldridge, Staffs, and Mr Hoyle is on the staff of the Association's Metallurgy division.

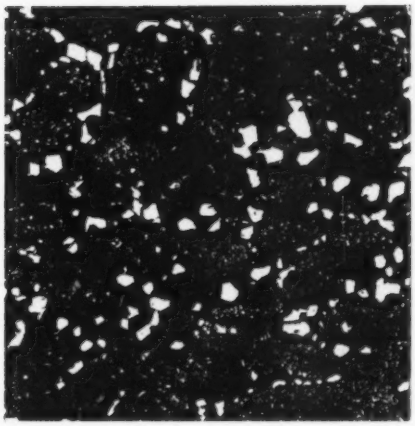
* There are many papers describing this process and only the original British Patent Numbers are given here.



1 *Typical 18–4–1 high-speed steel, as-cast (All micrographs are etched in 2:1 Nital)* × 100



2 *Typical 18–4–1 high-speed steel bar structure* × 100

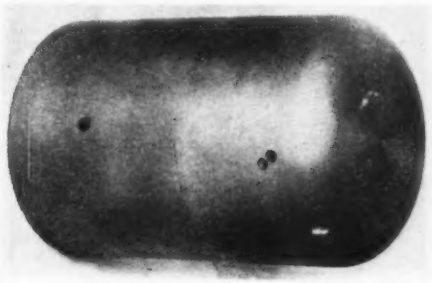


3 *Structure as in Fig.1 after treatment by the prespheroidizing process*

4%Cr–1%V–0·7-0·8%C) is shown in Fig.1, and is seen to contain a considerable proportion of eutectic both in the form of grain-boundary envelopes and individual colonies. This structure has been discussed in detail by Ineson and Hoyle.[3]

To produce from such a structure a material suitable for use in tools, it is generally considered necessary to eliminate all traces of the cellular carbide structure: this is conventionally accomplished by hot working, and a reduction of 90–95% (20:1–10:1) is normally considered necessary. In the course of this reduction large colonies of eutectic are broken up and elongated to form carbide stringers; a typical satisfactory bar structure is shown in Fig.2.

Up to the present time it has been stated, with the exceptions mentioned below, that the cellular structure of Fig.1 could not be dispersed by purely thermal means. Recently, however, Hoyle and Ineson[4] have devised a new heat-treatment cycle (Prespheroidizing) whereby complete freedom from eutectic can be achieved without any deformation of the ingot. Figure 3 shows the structure of an ingot so treated and the worked bar from a prespheroidized ingot is found to be virtually free from carbide stringers. Russian workers[5] have reported a similar thermal method for obtaining a change in as-cast structure; their method differs from Hoyle and Ineson's process in detail, but the underlying principles are similar. Nevertheless, the structures obtained by these methods are, in most cases, not suitable for use in tools, and hot work is still required.



4 *Extrusion slug with porosity and blowholes*

5 *Surface finish of typical extruded high-speed steel bar*

OBJECTS OF INVESTIGATION

The aims of the work to be described fell into three parts. In the first instance it was desired to examine the feasibility of producing high-speed steel bars, both round and flat, from as-cast material by extrusion instead of forging and rolling.

Once this had been accomplished, the aims were broadened to include the determination of the optimum heating temperatures and times, the examination of the effects of the prespheroidizing treatment, mentioned above, on the properties of the extruded bar and an attempt to combine a pre-treatment process with the heating for extrusion.

Thirdly, it was required to examine the properties of extruded bars, by laboratory tests and performance tests.

EXPERIMENTAL PROCEDURE

The extrusion experiments were carried out on a 400-ton horizontal, dead-weight accumulator, hydraulic press, kindly made available by the Department of Industrial Metallurgy of Birmingham University. The press had previously been used only for non-ferrous alloys (primarily aluminium base) and had to be retooled and specially fitted for steel extrusion. It is of a simple design, and all billet and bar handling was manual. However, the method of operation developed enabled the billet to be transferred from the furnace to the container and extruded within a reasonably short time.

The press was operated at full valve opening at all times, yielding a maximum ram speed under load of about 2 in./s.

The container had a bore of $2\frac{3}{4}$ in. dia. taking slugs of $2\frac{5}{8}$ in. dia. by 4 in. to 5 in. long. The lengths of the extruded bars varied from 2 ft 6 in. to 20 ft depending on the extrusion ratio. Glass was used as a lubricant throughout. An agglomerated glass-powder pad was placed between the die and billet while the hot billet itself was rolled in glass powder before being inserted into the container. The die itself had, in each instance, a conical face with a generous radius into the parallel throat. This type of die has been found most satisfactory for lubricated extrusion, both from experiments with other steels[6] and model experiments with plasticene.[7]

Slugs were prepared by casting into mild-steel drain pipes of 3 in. internal diameter by 3 ft long, slugs being then machined from the ingots produced. This method of casting was rather unfavourable and many of the slugs were extremely porous as shown in Fig.4. In spite of this, however, the slugs all extruded quite satisfactorily. It is almost certain that some of these would have burst if forged by normal practice.

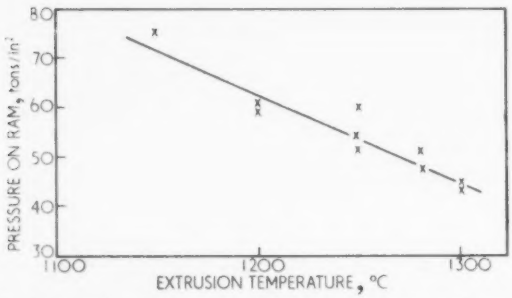
6 *Surface finish of cracked bar*

No particular precautions were taken to avoid clinking during heating. All the slugs were preheated to 300°C in the same furnace as the dies and from there they were transferred directly into the hot furnace, which for some experiments was as high as 1310°C. They reached a uniform temperature in 35–40 min. The high-temperature heating was carried out in an atmosphere of nitrogen to minimize scaling.

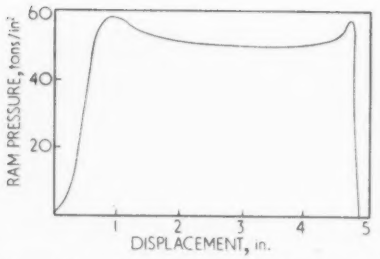
Each bar was extruded into a trough of vermiculite and left submerged until it had cooled to room temperature. This course was taken to retard the rate of cooling and minimize the danger of cracking during cooling or subsequent handling. All bars, unless otherwise stated, were annealed before examination. 18–4–1 high-speed steel was used throughout.

The range of sizes of the extruded bars included 1 in., $\frac{3}{4}$ in., $\frac{1}{2}$ in., and $\frac{3}{8}$ in. dia. (extrusion ratios of 7·6, 13·3, 30·2, and 53·8 respectively), produced from both as-cast slugs and slugs subjected to the BISRA heat-treatment process. The extrusion temperatures ranged from 1150° to 1300°C. (Note: furnace temperatures are quoted throughout this paper.) Some of the as-cast slugs were given a short soaking treatment after attaining the required temperature.

Some of the slugs extruded were taken from rather porous and segregated continuously cast bar. From these, and from the normally cast slugs, a limited number of extrusions was made to produce two $\frac{1}{2}$-in. dia. or two $\frac{3}{8}$-in. dia. bars simultaneously.



7 *Relationship between extrusion temperature and ram pressure, extrusion ratio 53·8:1*



8 *Typical variation of ram pressure with displacement*

RESULTS

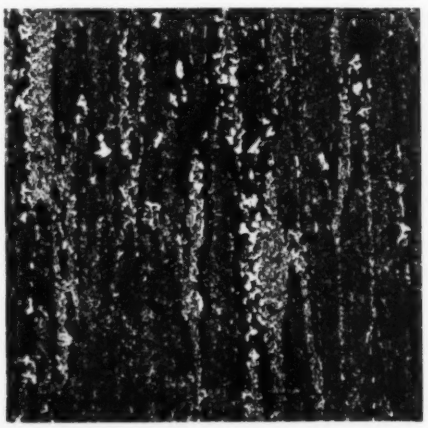
General

In spite of the porosity and generally poor quality of many slugs, all the bars extruded satisfactorily at temperatures between 1150° and 1280°C, looked sound externally and had a good surface finish (Fig.5). There was little to choose in surface finish between bars extruded in the temperature range 1200–1280°C. Those extruded at 1150°C were not quite as good, showing some tendency towards streakiness in the direction of the bar. This streakiness is probably largely due to the behaviour of the glass lubricant under the particular conditions as discussed by Sukolski.[8] Extrusion ratio in the range tested (7·6:1–53·8:1) had no detectable effect on surface finish.

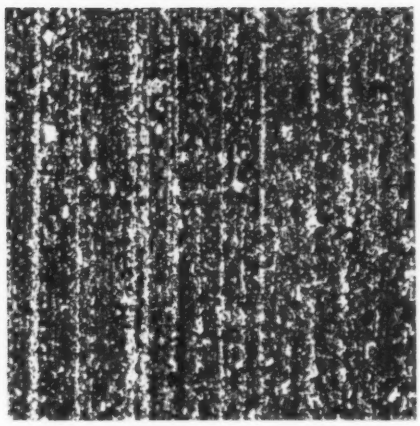
Bars extruded at 1300°C showed an extensive area of circumferential cracks (Fig.6). Bars from pre-treated slugs were worse in this respect than those from as-cast ones; bars from as-cast slugs were cracked over about the last third to emerge from the die and sound over the remainder of their length, whereas bars from pre-treated slugs were cracked over their whole length and in one instance the bar disintegrated from the die, being shot out in several pieces.

Extrusion pressures recorded showed expected trends, the range being from 30 to 74 tons/in² on the ram. Figure 7, for example, shows the variation of ram pressure with extrusion temperature for an extrusion ratio of 53·8:1. The pressure shown is the steady pressure during extrusion beyond the initial peak, when present. A typical pressure displacement diagram with an initial peak is shown in Fig.8.

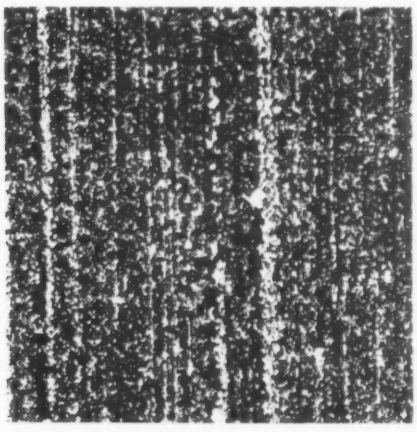
The dimensions of the bars were somewhat unusual,



9 *Structure of extruded 1-in. dia. bar* × 100



10 *Structure of extruded $\frac{3}{4}$-in. dia. bar* × 100



12 *Similar to Fig.11, but slug soaked at 1280°C, before extrusion* × 100

most of the diameters being some 0·005 in. smaller in the middle than at the ends. The explanation of this probably lies in variations in the speed of the ram during extrusion. The control valve for the ram was of the wheel type, being hand operated. This meant, and it was in fact confirmed by measurement over twelve extrusions, that the speed of the ram was greatest in the middle of the stroke. By the time the ram had built up to its maximum speed it was time to start closing the valve.

METALLURGICAL EXAMINATION

Bars from as-cast slugs

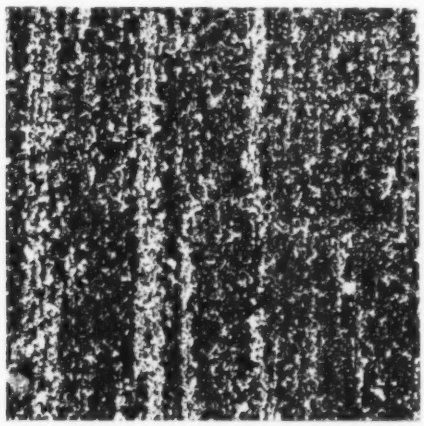
The structures of the bars were found to be in accordance with expectation. The effect of reduction on break-up of the carbide network is illustrated in Figs.9 and 10. It is apparent from the structures that the homogeneity is improved by increasing the reduction, as is the case in forging and rolling. Figure 9 shows that in the 1-in. bar there are still some remnant eutectic formations, again indicating that this reduction is probably inadequate. To ensure freedom from eutectic it was found necessary to extrude $\frac{3}{4}$ in. dia. or smaller bars, as in Fig.10. These results are exactly as would have been expected from forging and rolling and are included to demonstrate that extrusion itself imparts no unusual features to the structures.

The effect of extrusion temperature was, in general, also found to be small. Little change in macrostructure was observed when the temperature was varied between 1150° and 1280°C; 1300°C was, however, unsafe, as has been stated earlier.

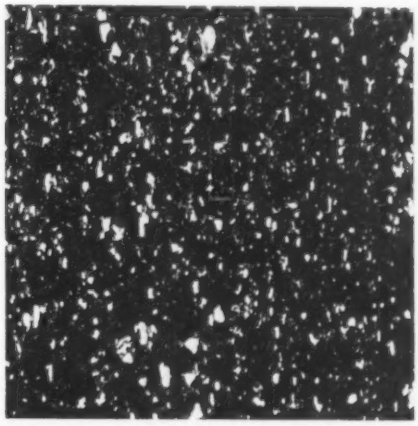
Microscopic examination showed that in bars extruded at temperatures above 1250°C structural changes had taken place. This has been discussed elsewhere,[4] and details are not, therefore, given here.

Because 1300°C was an unsafe extrusion temperature, experiments were made to examine the possibilities of achieving a similar effect at lower temperature, and it was found that this was possible at 1280°C. Samples were therefore extruded at this temperature and, to determine the effect of heating time, in one case an extra $\frac{1}{2}$-h soak was used (i.e. total time in furnace extended to 70 min). The results are shown in Figs.11 and 12 respectively. Satisfactory structures are observed in both cases and there appears to be a reduction in the severity of carbide stringers in Fig.12 (soaked). Drill tests, discussed later, suggest that the extra heating time resulted in enhanced performance.

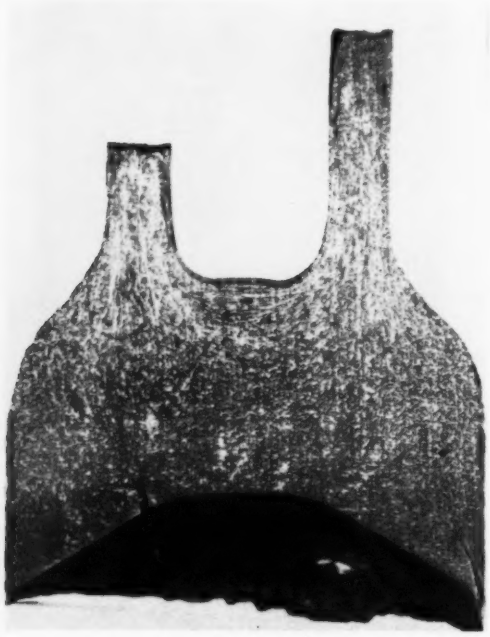
The inference from this work is that an extrusion



11 *$\frac{3}{4}$-in. dia. bar extruded from slug heated to 1280°C, not soaked* × 100

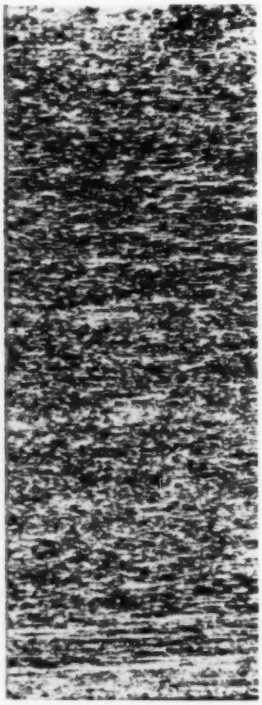


13 *$\frac{3}{4}$-in. dia. bar extruded from prespheroidized slug, as Fig.3* × 100

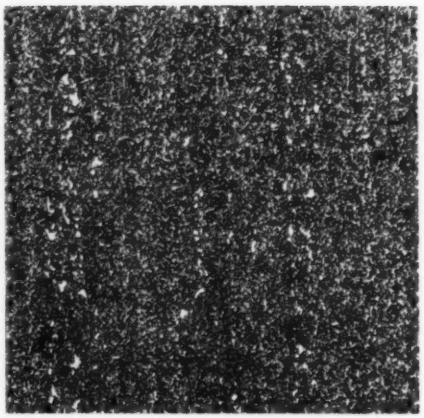


14 *Macrostructure of discard from 2-hole die extrusion*

temperature of 1280°C is apparently the optimum for 18–4–1 high-speed steel, and that a ½-h soak at temperature may give superior structure and properties.



15 *Bar from extrusion shown in Fig.14* × 10



16 *½-in. dia. bar extruded from continuously cast ingot* × 100

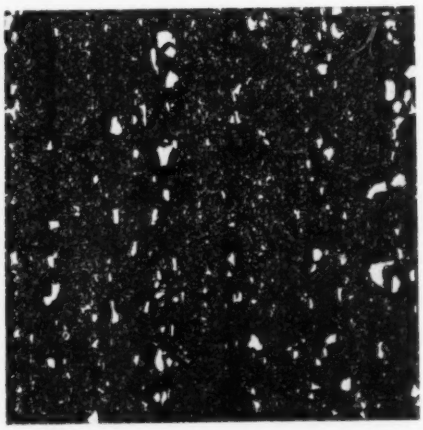
Bars pre-treated by the prespheroidizing process

It has been demonstrated that extended soaking time in heating can achieve a partial break-up of eutectic before extrusion. The bars produced are, however, still subject to the presence of carbide stringers. The heat-treatment cycle developed by Hoyle and Ineson[4] gives a more drastic change in structure. Several billets have been treated and extruded, and it has been found that the conditions for extrusion are the same as those required for as-cast billets, with the exception that extended soaking time is no longer required.

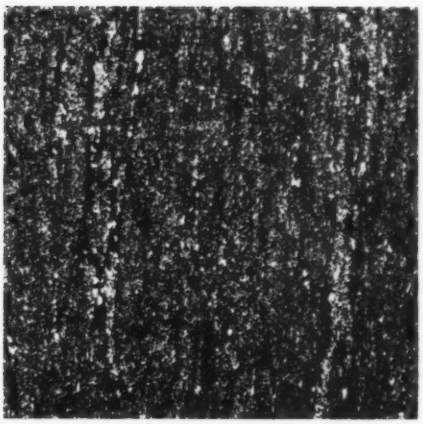
The structure of a typical extrusion from treated material is shown in Fig.13, which (although it is a longitudinal section) is completely free from carbide stringers and almost non-directional in character. This is offset to some extent by the presence of carbide particles that are larger and more angular than those usually found in wrought high-speed steel; it has yet to be established whether or not this modified structure is beneficial to the performance of the steel in service. It is hoped ultimately to combine the refining treatment with the heating for extrusion.

Special technique, two-hole dies

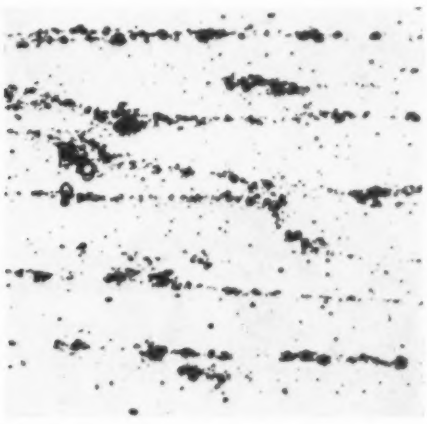
A number of extrusions was made through two ⅜ in.



17 *½-in. dia. bar (2-hole die) extruded from prespheroidized continuously cast ingot* × 100



18 *½-in. dia. bar from continuously cast billet (2-hole die)* × 100



20 *Effect of severe porosity in 1-in. dia. bar* × 500 (unetched)

dia. holes in one die and two ½-in. dia. holes in one die. The object of this was to extrude in such a way that the material at the centre of the ingot appeared at the outside surface of the bar, from where it could be removed by machining. Figure 14 shows the flow pattern in such an extrusion and it is apparent that material from the centre of the slug has been extruded to the inside surfaces of the two bars, which showed some porosity in this position. This particular slug was from an ingot top and the V-segregation rendered the mid-radial structure slightly worse than the centre with respect to carbide segregation. The worst part of the slug was therefore not brought to the surface of the bar. Figure 15 shows the macrostructure of a bar from a more normally segregated slug. Microstructures of both normal and pre-treated bars extruded by this method were substantially similar to those from single-hole extrusions.

This method, although perhaps not of great practical value, offers an alternative to the technique that has been used in the past to obtain a similar result, i.e. to section the ingot longitudinally and to work each portion separately. In cases where it is absolutely essential to ensure a bar with freedom from centre segregates the method would be worthwhile. The advantages may be offset to some extent by the greater risk of distortion in heat-treatment due to the unbalanced carbide structure.

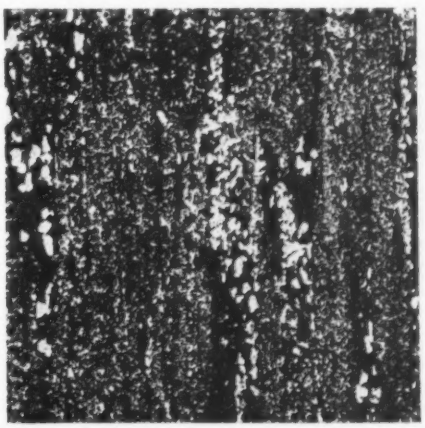
EXTRUSION OF CONTINUOUSLY CAST HIGH-SPEED STEEL

A small quantity of continuously cast high-speed steel, made on the plant and by the method described by Waters *et al.*,[9] was made available for these experiments. The structure of the billet was as described by Waters, and suffered from severe central porosity (note: the sample supplied was actually scrap material and these effects were probably more severe than usual). Four slugs were extruded at 1250°C, one prespheroidized and one as-cast into ½-in. dia. bar, and one prespheroidized and one as-cast into two ⅜-in. dia. bars simultaneously. In the ½-in. dia. bar some exceptionally good areas were found, both in normal and treated material (Figs.16 and 17), but there were also some less satisfactory structures, particularly in association with central porosity. This latter effect is demonstrated in Figs.18 and 19 which are of as-cast material extruded through the two-hole die.

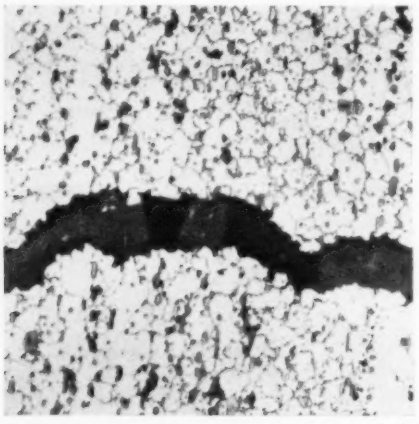
THE EFFECTS OF POROSITY

It has already been stated that some of the slugs were of poor quality in regard to soundness.

19 *As Fig.18, different field* × 100



21 *As Fig.20, etched nital* × 100



22 *Structure of bar through crack, as-extruded from 1300°C* × 500

During the experiments particular attention was paid to the behaviour during extrusion of internal cavities in these slugs. Central cavities, if exposed at the ends of the slugs, inevitably became oxidized during the heating operation and produced structures like that shown in Fig.20. Further examination of the sample shown in Fig.20 revealed areas of differential etching (Fig.21) which are believed to be associated with porosity on a smaller scale.[10] In general, however, samples were free from both those effects, suggesting that blowholes, known to be present in the ingot, had been completely healed.[11]

The bars fractured cleanly and no porosity was thus found without the presence of oxide. The 1-in. dia. bar tended to give a 'woody' fracture, but all others gave smooth, clean ones. In particular, the $\frac{3}{8}$-in. dia. bars gave very fine silky fractures.

From the experimental results, therefore, it would seem that unoxidized blow-holes heal satisfactorily on extrusion, but that where oxidation has taken place the healing is not entirely satisfactory.

THE EFFECT OF COOLING RATE

As mentioned earlier, attempts were made to retard the cooling rate by extruding the bars into vermiculite. Because of bar clearing and handling difficulties this, for some parts of the bars, was probably not as effective as was hoped and it was apparent, from samples examined before annealing, that some as-hardened types of structures were obtained. Figure 22 illustrates this and, although made primarily to show the intergranular transverse cracking in a bar extruded at 1300°C, it also shows a reasonably fine grain size.

The possibility was therefore considered that a satisfactory hardened bar of 18–4–1 high-speed steel might be produced by controlled cooling from the extrusion temperature. The rate of cooling may need to be controlled to avoid cracking which has been known to occur, and it would be an advantage if the bars could be straightened before cooling. In this initial series of experiments all the bars were cooled in vermiculite. It was, however, decided to check the hardness of the un-annealed bars, and the results are shown in Table I for these and for the same specimens after single and double tempering. In most cases the hardness levels are shown to be acceptable for most applications. There are, however, one or two discrepancies, which will be discussed below. The main observation is that a sample of satisfactory hardness and structure can be obtained in 18–4–1 high-speed steel by hot extrusion, cooling, and double tempering without the need for intermediate annealing and heating for hardening.

TABLE I Properties of as-extruded and tempered HSS bars

Heating time, min	Extrusion temp., °C	Initial condition	Vickers hardness numbers As extruded	+ Temper* 1 h 560°C	+ Temper* 1 h 550°C
40	1300	As cast	769	823	836
40	1280	„ „	754	817	817
70	1280	„ „	801	847	840
40	1250	„ „	736	804	752
70	1200	„ „	730	846	856
40	1300	Pre-spheroidized	827	881	862
40	1250	„ „	746	827	852
40	1200	„ „	752	826	817

* Cumulative tempering treatments

THE PROPERTIES OF EXTRUDED HIGH-SPEED STEEL BARS

Two methods of testing have been employed, on a rather limited scale, to determine the properties of extruded high-speed steel bars, a laboratory test and a drill performance test.

Bend tests

For these tests, the bend test described by Hoyle and Ineson[11] was used. This test employs a specimen 3 in. × 0·1 in. × 0·2 in., which is subject to bending under 4-point loading.

It is considered that this gives a reasonable assessment of the overall 'toughness' of a tool steel, the factor 'Q' being a combination of strength (L/θ), elastic behaviour (L/θ), and ductility (Y).

The results obtained are given in Table II. Two sets of specimens were tested: (*a*) samples as-extruded and double tempered, and (*b*) samples extruded and annealed, hardened from 1280°C and tempered as in (*a*). Both sets of specimens were from a flat bar extruded at an indicated temperature of 1280°C. A further set of results (*c*) is included in Table II to give typical properties from conventionally treated 18–4–1 steel.

The results from series (*b*) are typical of average quality 18–4–1 steel treated to this particular hardness level, which is considered by many users as the

TABLE II Bend test results on extruded HSS bars

Property	(a) Samples extruded, double tempered 560°C	(b) Samples extruded, annealed, hardened, double tempered 560°C	(c) Typical conventionally produced bars
Vickers hardness	827	861	854
Limit of proportionality, L, tons/in²	118	127	163
True breaking stress*, θ, tons/in²	141	148	176
Ductility factor*, Y, %	45	37	25
L/θ (strength factor)*	0·75	0·73	0·94
L/θ (Yield ratio)*	0·84	0·86	0·93
Q ('quality' factor)* $= L/\theta \times L/\theta \times Y$	27	20	22

* Parameters developed by Hoyle and Ineson[9]

TABLE III Bend test results on continuously cast high-speed steel

Property	Outer zone	Inner zone
Vickers hardness	875	847
Limit of proportionality, tons/in^2	112	112
True breaking stress, θ, tons/in^2	131	139
Ductility factor, Y, %	32	47
L/θ (strength factor)	0·66	0·66
L/θ (yield ratio)	0·90	0·80
Quality factor, Q	17·5	23·5

optimum for general purposes. The improvement in Q value in series (*a*) is due to the higher ductility (Y), which in turn is consistent with the lower hardness value (this may be considered too low by some users). From the limited data, therefore, it may be concluded that high-speed steels treated by these methods have satisfactory physical properties insofar as they are revealed by the bend test.

Some tests have also been made on continuously cast and extruded bars. In this instance it was desired to compare the properties of the central and outer portions of the bar, i.e. material originating respectively from porous and sound material. The results are given in Table III and are discussed in greater detail elsewhere.[10]

Drill tests

Two samples of $\frac{3}{8}$-in. dia. bar extruded at 1280°C after normal heating and with $\frac{1}{2}$-h soaking time respectively, were subjected to drilling tests. From each bar, two jobbers drills, $\frac{5}{16}$ in. dia., were made, and heat-treated alongside two normal drills from a production batch. The test used was similar but more severe than the British Standard drilling test: 10 holes are drilled at each of increasingly severe conditions of speed and feed, these levels being arbitrarily designated in this paper as *A*–*E* inclusive. Survival of level *C* is normally expected.

Results are given in Table IV for these drills, including a duplicate experiment made after re-pointing.

These results suggest superiority of the extruded samples and in particular of the specially heated bar, over forged and rolled material. It must, however, be borne in mind that the results cannot be considered as statistically significant because of the small number of samples. Nevertheless, the results were very encouraging, the drills were probably at least the equal of good conventionally produced drills.

CONCLUSIONS

It has been shown that high-speed steels can be extruded from as-cast material, at temperatures higher than those considered feasible for forging and rolling.

A partial break-up of the eutectic has been observed at the temperatures used, especially with extended heating time and this gives greater freedom from carbide stringers in the extruded bar.

Extrusion of unsound high-speed steel, both statically and continuously cast, has been shown to be feasible, and pipe has been found to heal during extrusion when atmospheric oxidation is absent.

TABLE IV Results of drill tests

Condition of bar before making drill	Test I No. of holes at level C	D	E	Test I (re-pointed) No. of holes at level C	D	E
Heated 40 min	10	3*	...	10	8*	...
extruded and annealed	10	1*	...	10	3*	...
Heated 70 min	10	8*	...	10	7*	...
extruded and annealed	10	10	5*	10	10	10*
Annealed bar taken at	8*	...	...	10	7*	...
random from forged	10	6*	...	10	10	2*
and rolled stock						

* Denotes corners worn. Test I was with machine-pointed drills and Test II with the same drills hand pointed.

Bars extruded, cooled, and double tempered have been found to give hardness, structure, and bend-test properties satisfactory for normal applications, suggesting that it may be feasible to eliminate the hardening operation for certain simple shapes or components.

Extruded high-speed steel bars after conventional heat-treatment gave better performance in drill testing than two random samples of conventionally forged and rolled high-speed steel of nominally the same composition. Further work is required to prove this on a statistical basis.

High-speed steel ingots treated by the new prespheroidizing process have been satisfactorily extruded, to give structures virtually free from carbide stringers.

ACKNOWLEDGMENTS

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The extrusion of steel tubes

J. W. R. Naden

SYNOPSIS

This paper describes the hot extrusion of steel tubes by the direct method, using hydraulically operated horizontal presses. It also deals with raw materials, billet preparation, the heating of the billets, and lubrication of the tools, and points out some advantages over other methods of hot-working. Reference is made to tool steels and tool designs, and to some of the problems that arise.

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INTRODUCTION

EXTRUSION is one of the best ways of making tubes. It is an excellent method of making composite tubes, e.g. those that are stainless outside and mild steel inside, or vice versa. It is possible to make tubes with the outside and inside of the same material and to have a layer of a different material in between; many variations of the sandwich form are possible. It should be noted that the individual layers become welded to one another during extrusion. No doubt such tubes will have many applications in future.

Extrusion is also a good way of making solid and hollow shapes; in fact, it is the only way of making certain hollow shapes in one piece, without building them up by welding, etc. All types of steel and highly alloyed materials that can be hot-worked by hammering, rolling, pressing, etc. can also be extruded. With some steels and alloys that are very difficult to hot-work, it is possible to complete the hot-work in one operation and at practically the same temperature by extrusion, whereas a number of heats with considerable variation in temperature would be necessary by other methods.

The usual way to make a tube by direct extrusion is to place a hot billet in a container and to force it through an annulus formed by a die and a mandrel (Fig.1). Extruded tubes from about 1 in. o.d. × 0·1 in. thick to 7·5 in. o.d. × 0·75 in. thick are made using presses of about 1000–3000 tons capacity. Tubes smaller than 1 in. o.d. can be made either by using the cored extrusion method or by using smaller-capacity presses and comparatively small-diameter billets. A cored extrusion is made by using a billet into which a central Mn–Ni steel core has been fitted. The billet is extruded as a solid, the core being removed afterwards by stretching.

The diameter, thickness, and length of an individual tube will depend upon the material, the power of the press, and the ability of the tools to withstand the loads that are necessary to make it.

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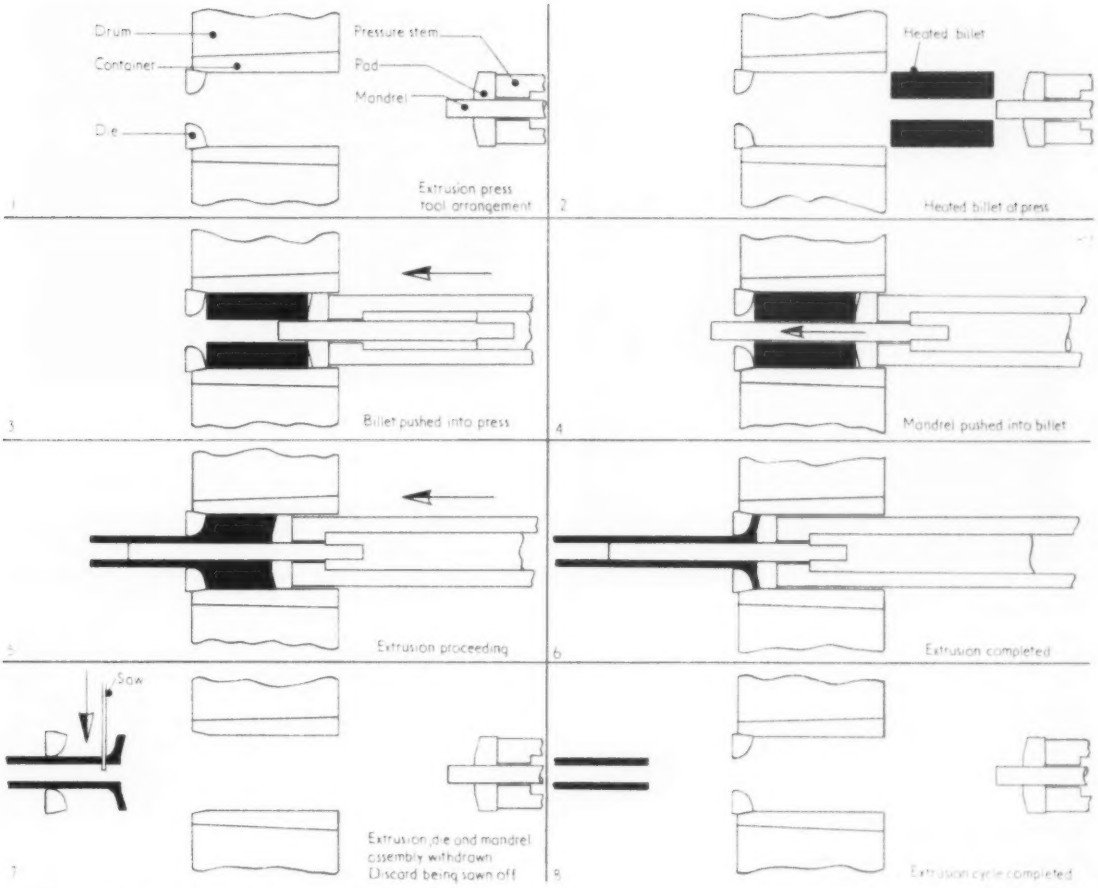
RAW MATERIALS

Generally the raw materials are in the form of rolled or forged round bars. For some materials, however, it is feasible to start with cast ingots. It is customary to use machined bars, particularly when using expensive materials, such as stainless and highly alloyed steels, and those that are difficult to hot-work. In these cases the surface finish should be very good, 150 μin. or better; otherwise, there is some risk of a poor surface on the extruded tubes.

So far as carbon and non-stainless steels are concerned, it is possible to obtain satisfactory results with unmachined bars. The general practice in these cases is to pickle the bars and to remove any surface imperfections by 'dressing' locally. Whether machined or unmachined bars are used must depend on the quality of the product required and on the economics.

Although round bars are commonly used, it is possible to use other shapes, such as squares with rounded corners and gothic sections, but the yield of tubing is likely to be slightly less from billets cut from bars other than round, because more discard will have to be removed from the front ends of the tubes. The slight loss in yield may, of course, be offset by economic factors.

Cast machined bars and billets, in some cases prepared from solid ingots and in others from centrispun



1 *Diagram showing sequence of operations*

hollows, have been used with considerable success. This is particularly so with ingots made from high-speed steels and some carbon steels. A reasonable measure of success has been achieved using 'as-cast' stainless and highly alloyed steels but with such types the surface of the tubes is not as good as when they have been made from forged materials, and they often require some rectification.

It is well worthwhile balancing the composition of the nominal 18–8 series of stainless steels so as to make them fully austenitic, i.e. to avoid the formation of δ-ferrite, which can result in transverse breaks during extrusion. In most specifications the stipulated ranges for Mn, Ni, and Cr are wide enough to allow the balancing to be achieved by keeping Mn and Ni towards the top end and Cr towards the bottom end of the specified limits.

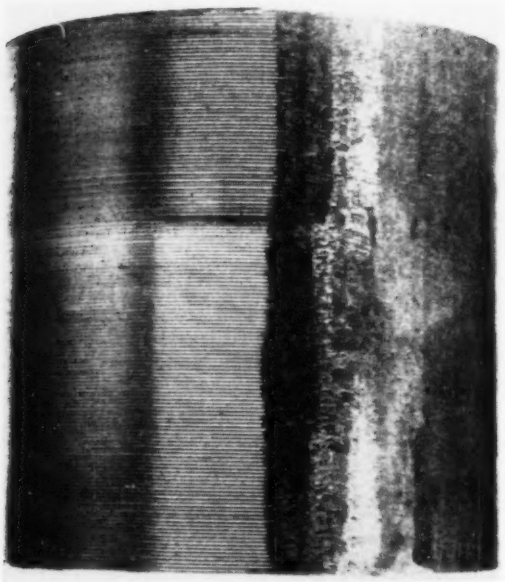
BILLET PREPARATION

It is true that the better the billet surface, the better is the quality of the extruded product. Imperfections on the billet surface, including rough-machine marks, dents, etc. result in imperfections in the tube, and so it is worth ensuring that the billet surfaces are good. It is much easier to make good the billet than to rectify the tube after extrusion; this is especially so in the case of the bore. A rough-machined billet had part of the turning marks removed by a hand grinder, and the effects of the different surface conditions are shown in Figs.2 and 3.

Billets are cut from the bars by means of saws, abrasive discs, or any suitable method that will leave smooth and square end faces. It is just as important that the end that faces the die should be as smooth as the body of the billet, since it becomes the front end of the tube. The end should also be reasonably square. Out-of-square and rough ends are likely to make 'dressing' of the leading ends of the tubes necessary and to lead to heavy discards. The effects of rough and smooth end faces on the surface finish of the tubes are shown in Figs.4 and 5.

When difficult materials are to be extruded, the corner of the leading end of the billet (i.e. the end that is pushed through the die first) should be radiused. This radiusing will often prevent breaks in the corresponding area of the tube. Fine machine marks in the form of sharp grooves can also initiate transverse breaks in the tubes.

The billets must have a hole through before extrusion; this can be made by drilling or machining, by drilling and hot-expanding, or by hot-piercing. All three methods are in use, the choice depending upon



2 *Billet part rough-machined and part hand-ground*

the bore size and length of billet, and to some extent on whether the material is cheap or costly. For example, it may be cheaper with carbon steels to drill or machine the hole, even though it is fairly large, than to try and save material by drilling a small hole and hot-expanding it to the required diameter.

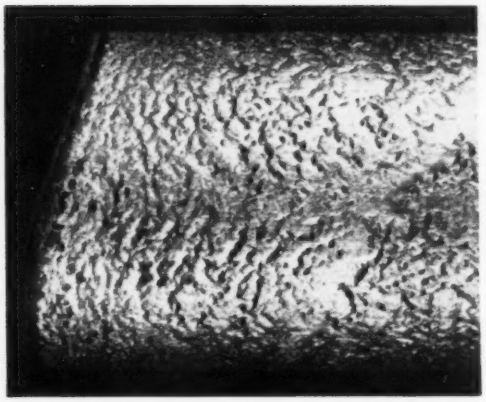
Drilling or machining

These methods are used where the bore is small, say up to about $1\frac{1}{4}$ in. dia., when the losses of material are not very great and it would be more expensive to prepare the bore by any other means. Some steels are difficult to hot-expand or hot-pierce; in such cases the billets are either drilled or machined.

Drilling and hot-expanding

This method is used for bore sizes between about $1\frac{1}{4}$ in. and 3 in. dia. In such cases a small hole of $\frac{3}{4}$–1 in. dia. is drilled; the drilled billet is then heated and a special shaped plug is forced through to expand the bore to

3 *Surface condition of tube made from billet shown in Fig.2*



4 *Effect of rough end face on leading end of tube*

the required dimension. This method has two distinct advantages when compared with hot piercing:

(i) a longer billet can be drilled and expanded than can be hot-pierced, and this can result in a better yield. However, although there is no limit, within reason, to the length of the billet that can be drilled and expanded, there is a limit to the length that can be extruded; over a certain length, eccentricity occurs in the middle of the tube due to the billet staving at each end in the container and causing the mandrel to bend in the middle

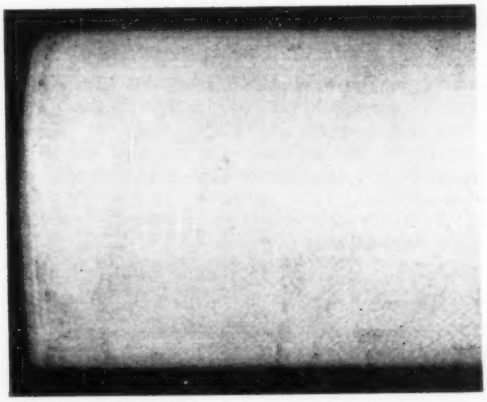
(ii) a concentric hollow billet can be achieved readily because it is easy to drill the hole concentric with the o.d. of the billet and this acts as a guide when expanding.

Bores larger than 3 in. dia. can be made by this method, but it may be necessary to carry out more than one expanding operation.

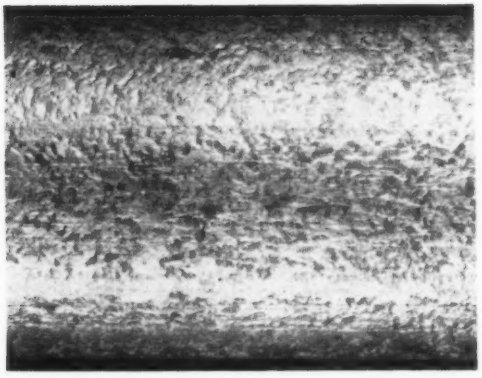
Hot-piercing

This method may be used when the bore is 3 in. dia. or greater. Hot-piercing would seem to be the ideal method, since if there were no snags, the yield would be greatest.

It would be a great step forward if billets could be pierced concentrically and extruded in the same heat, i.e. by piercing in the extrusion press against a blank die using a piercing nose on the mandrel, quickly changing the die, and following through with the extrusion. Although at the present time piercing can be carried out in the extrusion press, it is not possible to



5 *Effect of smooth end face on leading end of tube*



6 *Tube made from scaled billet*

7 *Tube made from non-scaled billet*

do it in the way described; an open die is used, which allows a comparatively large slug of material to be pushed out, and this greatly reduces the yield. Because of this, it is usual to carry out piercing as a separate operation, and in practice the length of the billet that can be pierced with reasonable concentricity is limited to about 6–7 times the bore. Even at this ratio it is very difficult to keep eccentricity within reasonable limits, and for continuous production it is worthwhile using a billet length of about five times the bore in order to minimize it. If the eccentricity is greater than can be tolerated it is necessary to rectify by machining, which is costly.

Where bores of 3 in. and upwards are required, every effort should be made to hot-pierce, as by this method great savings are possible.

BILLET HEATING

The ideally heated billet is one uniformly soaked for a short time at the correct temperature and free from scale. This applies whether the billet is to be hot-expanded, hot-pierced, or extruded, and particularly in the last-named case, as the tube may be the finished product and so it should be as good as possible. The necessary conditions are very difficult to achieve in works practice but they are well worth striving for, as the product from such a billet is far better than that made from one that is unevenly heated and scaled. Satisfactory tubes, etc. can be made from unevenly heated billets but not from scaled ones, as in the latter case the surfaces will be poor (*see* Figs.6 and 7, showing tubes made from scaled and non-scaled billets). If a billet is scaled, the scale will break during hot-working and score the tools, which in turn will spoil the surfaces of the expanded or pierced billet or the extruded tube. Scale is definitely to be avoided, since not only does it spoil the tools and the product, but it also prevents proper lubrication and reduces the yield.

Temperature control should be good, and the actual temperature should be varied to suit the type of steel being extruded. With some stainless and special types the most satisfactory results are obtained when hot-working is carried out within a limited temperature range, say $\pm$ 10°C from the optimum.

At the present time the following types of furnaces are in use:

1 open gas-fired without controlled atmosphere
2 open gas-fired with controlled atmosphere
3 salt baths
4 glass baths
5 induction.

Open gas-fired furnaces without controlled atmosphere

A number of such furnaces are in use, some being used for preheating and others for the final heating to the extrusion temperature. They consist of roll- or push-through, rotary, and batch types.

Such furnaces are satisfactory as far as temperature and soaking are concerned, but the billets heated in them are covered with scale. Some measure of protection against scale is afforded by coating the billets with a frit, but the frits with which the author is familiar have not been complete preventatives at 1000°C and over. Such coatings work better with stainless and heat-resisting types than they do with non-stainless types. The reason for this may be as follows:

> The frit, which is mixed with a carrier, does not become viscous until a fairly high temperature (700–800°C) has been reached, and during the heating period the carrier evaporates, leaving the frit as a powder on the billet surfaces. In powdered form the frit will not prevent scaling, and the amount that can occur before the frit becomes viscous and forms a coating can be appreciable, particularly with non-stainless steels. As the frit becomes viscous it combines with the scale to form beads which break the coating, thus leaving the surfaces partly unprotected. The more scale there is present, the poorer would be the protection afforded by the frit. If this is the true explanation, it may be the reason why frits work better with the stainless types, since with such steels the scale is much less and takes longer to form than in the case of the non-stainless types.

Where this type of furnace is used, carefully controlled time cycles are necessary to ensure that the billet is in the hot part of the furnace for as short a time as possible.

Open gas-fired furnaces with controlled atmosphere

The importance of obtaining scale-free billets is realized in many quarters, and efforts have been made to build furnaces to do this. A number of such furnaces are in use in which reducing conditions are achieved by burning only a portion of the gas. In some, part of the gas is burnt on the hearth, the remainder being burnt at the charging end of the furnace to preheat the billets.

Such furnaces are a definite step forward, but as yet they do not give a complete answer to scale-free heating; some of the reasons for this are:

(i) to keep the conditions on the hearth reducing, a positive pressure must be kept in the furnace, but as soon as a door is opened to charge or discharge a billet infiltration of air must occur, and this upsets the reducing conditions. In addition to this, gaps are bound to form in the brickwork due to distortion of the frame, etc., and allow air to get into the furnace. It is also very difficult to maintain a positive pressure on the low gas input (i.e. during plant breakdowns); these are the times when the billets need protection from scaling

(ii) during combustion, moisture is formed, which to some extent upsets the reducing conditions and assists scaling

(iii) non-stainless steels heated in such furnaces are considerably better so far as scale is concerned than when heated in open gas-fired types. The improvement is not so marked when stainless steels are heated, one reason for this probably being that the chromium in the stainless steels is preferentially attacked.

Experiments are being made with a gas-heated muffle furnace with controlled atmosphere for the final stages of heating (i.e. in the temperature range where scaling would occur), and the results are very promising. In such furnaces uniformity of temperature and almost complete non-scaling conditions can be achieved.

Salt baths

The salt is basically barium chloride and the baths are electrically heated. Several are in use, and in general the products made from billets heated in them are quite satisfactory. Some advantages are:

(i) the billets are rapidly and uniformly heated

(ii) the billets are afforded protection from scale during heating; in fact, the salt will dissolve any scale already on them before they are put into the bath

(iii) the film of salt that sticks to the billet on its removal from the bath gives protection from scaling whilst it is being transported to the next operation.

Some disadvantages are:

(i) salt baths are inefficient and costly to run and the maintenance costs are high. The costs can be reduced by preheating the billets in a gas furnace, thus decreasing the time they have to be in the bath

(ii) quite a lot of salt is lost by 'drag out'

(iii) sludge has to be removed from the bath regularly, otherwise it will contaminate the bath and build up, causing shorting of the electrodes

(iv) billet surfaces are pitted if they are left too long in the salt

(v) billets must be completely dry. If they are not, explosions may occur when they are put into the salt bath.

Glass baths

A revolving furnace in which the refractory lining is coated with a thick layer of glass has been patented and is known as the Balestra furnace. It is in use in Italy, and there is some prospect that it may be used in the USA. Products made from billets heated in this way are very good. The furnace is a gas-fired continuous type, the billets being charged at one end and discharged at the other, and during their passage through the furnace they are constantly rolled in molten glass. Practically speaking, they are protected from the time they are put into the furnace until they are discharged. The glass dissolves any rust or scale that may be on the billets before charging.

Induction

A number of induction heaters are in use and a great deal of interest is being shown in them, particularly the low-frequency types; in fact, the use of these units is spreading rapidly. There is very little published information on the heat distribution in a billet heated by induction, but recently experiments have shown that the heating is nothing like as uniform as was once thought. Even so, products made from billets heated in this way are considered to be very satisfactory.

Induction heating lends itself very well to plant layout, and some advantages are as follows:

(i) the heat cycle is very rapid (of the order of a few minutes), which minimizes scaling

(ii) it is easy to flood the coil with an inert gas to prevent scaling, but this has not been found necessary with stainless and heat-resisting steels

(iii) the temperature can be varied with very little trouble; for example, it is easy to change from 1 200°C to either a much higher or a much lower temperature, whereas with the other types of heating such changes take quite a long time

(iv) should there be a plant breakdown, only a few billets are likely to be affected, as it is comparatively easy to stop the heating. This is not so easy with other types of heating, where generally a lot of billets will be involved.

There are, however, a number of problems to be solved before induction heating can be considered satisfactory. Some of them are:

(i) the major problem is the heat distribution in the billets, since using present-day equipment the temperatures axially and radially are not uniform

(ii) there is a danger of overheating certain portions of billets. This is thought to be particularly so with hollow billets when using low-frequency heating

(iii) the length and diameter of the billets must be kept reasonably constant or the heat pattern will be affected, and in addition there will be a number of electrical changes to be made. Electrical alterations are also necessary when changing from solid to hollow billets, and when changing from non-stainless to stainless steels

(iv) temperature measurement and control are difficult.

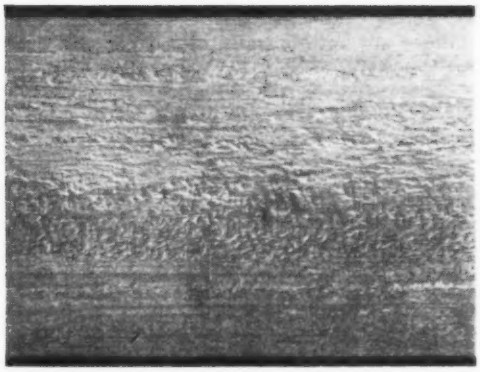
LUBRICATION

Every time an extrusion is made it is necessary to lubricate the container, the die, and the mandrel in some way to avoid 'seizing', which would almost certainly occur without lubrication; in fact, it is unlikely that a hot extrusion could be made if the tools had not been lubricated. The lubricants in general use are graphitic ones and glass, the latter being covered by the Ugine–Séjournet patent. For about 20 years, before glass became a practical proposition, graphitic lubricants only were used, and even though glass is now generally used, there are still instances when it is worthwhile using a lubricant such as graphite. Two specific cases are mentioned below.

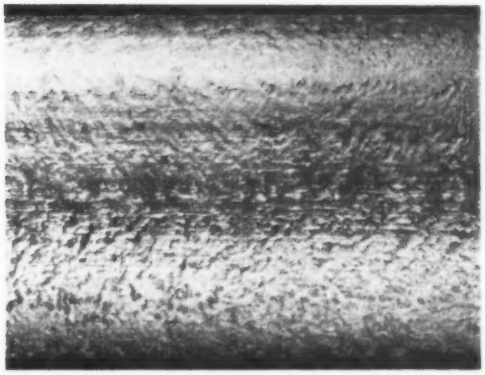
Graphite

For hot extrusion a first-class quality flake graphite is needed. It is usually mixed with a carrier such as oil or tar before being applied.

When the billets are coated with scale (i.e. for non-stainless types heated in open gas-fired furnaces), it is



8 *Tube made from scaled billet using graphite*



9 *Tube made from scaled billet using glass*

much better to use graphite than glass, as the surface finish will be much smoother (*see* Figs.8 and 9).

When billets are to be used with small bores, say up to $1\frac{3}{4}$ in. dia., and it is not worthwhile preparing them in any other way except by machining, graphite can be used with a marked advantage so far as yield is concerned. The reason for this is that with graphite it is possible to expand the bore of a billet in the extrusion press immediately before extruding the tube; it is not necessary to reheat the billet between operations. This is because the graphite can be applied to the mandrel. The mandrel carries a larger-diameter plug on its leading end; when the plug is pushed through the bore to expand it, the lubricant is not wiped off the mandrel and so the graphite is available for the extrusion. It has not proved possible to use this method with glass, as it is very difficult to apply it to the mandrel; it is usual to put it in the bore of the billet. This makes it necessary to allow clearances between the bore of the billet and the mandrel to prevent the glass being wiped from the bore when the mandrel is moved into position. This means that a larger-diameter hole is necessary, with a consequent reduction in yield.

Graphite can also be used for the stainless types; indeed, for about 20 years it was used to extrude practically all the stainless tubes made in the UK, and probably the bulk of similar tubes made elsewhere. When stainless steels are extruded using graphite, carbon pickup occurs and the carburized layer must be removed to avoid troubles such as weld decay, etc. during subsequent processing. The carburized layer is removed either mechanically or by pickling, which results in some loss of metal and consequently a lower yield.

Glass

The use of glass was patented about 18 years ago, and came into prominence about 10 years later; it has been applied increasingly since then. It can be used in various forms, as sheet glass, glass cloth or wool, fibre sheet, or powder. Nowadays it is mostly used in powdered form because this has certain advantages.

The powdered form is cheap and glasses can be used as powders that could not be used in the other forms, as it was not possible to draw them into fibres. Powdered glasses can be mixed with a binder and moulded into various shapes, which means that the glass can be placed where it is most likely to be effective. In addition, glasses having different viscosities can be mixed.

Some advantages of glass when compared with graphite are:

(i) it acts as an insulator and prevents the billets from being severely chilled when they come into contact with the tools. This in turn helps to avoid transverse breaks on the tubes
(ii) because of the insulation that glass gives to the tools, their lives are lengthened
(iii) thinner and smaller-diameter tubes can be made, i.e. greater extrusion ratios are possible
(iv) it is clean in working
(v) no carbon pickup occurs
(vi) it is viscous at the working temperature and because of this keeps feeding through the die, which means that very long extrusions can be made. For this reason heavier billets can be used, thus increasing the yield
(vii) it has made the extrusion of sections and hollow shapes with comparatively sharp corners practicable on a production basis.

Some of the disadvantages when compared with graphite are:

(i) it is more costly to use
(ii) it is more difficult to remove from the tubes
(iii) the surface finish of the billet has to be very good (better than when using graphite)
(iv) the billets must be, practically speaking, free from scale, otherwise the tubes will have much poorer surfaces than if graphite had been used.

It is obvious that the advantages of using glass far outweigh any disadvantages and that the use of graphite is only worthwhile in special circumstances.

EXTRUSION

The actual extrusion of the billets takes only a few seconds. The extrusion cycle is as follows:

(1) lubricate (die, mandrel, and container, if using graphite, or die, outside, and bore of billet, if using glass); (2) place billet in container; (3) extrude; (4) push tube and discard out of container; (5) cut off discard; (6) remove tube from press; (7) cool tools; (8) examine and clean the tools; if they are satisfactory the cycle is repeated from the lubrication as before.

Some of the important points are:

(i) it is necessary to preheat the tools before use and to cool them during use. They must not be overcooled; this applies particularly to the mandrel, as overcooling can cause transverse breaks in the bores of the tubes. On the other hand, if they are not sufficiently cooled they will

become too hot during the extrusion and pickup may occur between the tools and the tube, thus causing longitudinal scoring. In addition, the lives of the tools will be shortened

(ii) the die and mandrel should be checked for wear and for surface imperfections after each extrusion. The other tools should also be examined from time to time during the course of a shift

(iii) lubrication of the tools and the billet must be complete, otherwise the tools and the tube may be severely damaged because of pickup

(iv) the tubes must be regularly checked for concentricity, at, say, every tenth extrusion. Eccentricity is a serious problem and may be due to bent mandrels, misalignment of tools, uneven heating of billets, faulty lubrication, etc.

TOOLS

In the main, the expendable tools are dies, mandrels, containers, pressure pads, and piercer points. Generally the tools are made from forged hot die steels, but cast ones are gradually being used more and more. This is especially so with dies, but such items as cast die-support rings, pressure pads, piercer points, and even containers are being tried. The most popular steels for tools generally are 5%Cr–Mo–W–V and 9–10%W–2½–3%Cr. There are many other types in use, but these are the most common. The most expendable tools are dies, mandrels, and containers, and some details that may be of interest are given below.

Dies

Dies can be in single pieces or built up, i.e. a die insert, a support ring, and a die backer. The use of die inserts saves money, as the support ring and backer need not be made from the expensive hot die steels. Inserts can be made in more than one piece, and this can be advantageous when extruding sections or hollow shapes, as the pieces can be cleaned much more easily than a one-piece die. It is often easier to make a die in more than one piece as far as machining is concerned.

Dies can be built up by welding as they wear, and this repair work can often be carried out a number of times on the same die. A satisfactory electrode for this purpose is the 25%Cr–20%Ni type, and dies built up with this steel will give additional lives equal to the original one. It is obvious that building up dies can save time and money.

It is worthwhile having a number of dies in circulation and changing them after each extrusion. This will allow the dies to cool naturally, which is much better than having to cool them by air or water, which causes severe thermal shocks and sometimes cracks, shortening their lives. Changing the die after each extrusion allows it to be well inspected and any imperfections to be rectified before it is used again.

Mandrels

Mandrels are subjected to very severe working conditions. During an extrusion they become very hot and highly stressed in tension. As it is not easy to change mandrels after each extrusion, it is necessary to cool between extrusions to prevent them necking or being pulled in two. The cooling has to be as uniform as possible to prevent distortion, and the use of a fine water spray all round the mandrel has proved satisfactory.

Containers

Containers are not generally cooled between extrusions; there is very little to say about them except that they are very expensive and that every care should be taken to see that they are properly lubricated and cleaned after each extrusion.

FINISHING OPERATIONS

The tubes are trimmed at each end and then cleaned. The cleaning can be done in several ways: by abrasion, by immersing in caustic baths, by pickling, or by a combination of these methods. Caustic baths and pickling are the methods most commonly used. Caustic baths do not remove metal, which means that if graphite has been used for lubrication, cleaning in a caustic bath must be followed by pickling, to remove the carburized layer.

The tubes are checked for concentricity and examined. Any surface imperfections are removed by 'dressing' locally. After this they are heat-treated and then finally cleaned, usually by pickling. Samples are taken to check mechanical properties, hardness, and susceptibility to intergranular corrosion. The last operations before despatch are straightening and spectroscopic examination to ensure that the steel is as ordered.

GENERAL

Extrusion is now much more widely used than it was a few years ago, and presses have been and are being installed in many countries. The increased use of extrusion has resulted in press-makers improving their designs and adding many refinements, such as die and mandrel alignment equipment, quick die and tool changes generally, and better means of severing the discard and of removing the hollow from the press. In addition, modern presses are quick acting and highly mechanized generally.

No doubt the increase in the use of extrusion is in part due to certain advantages, some of which are as follows:

(i) it facilitates the production of quantities that are too small to justify economically the cutting of special rolls, as well as of pieces so awkward in shape that other methods would be inapplicable

(ii) variations in size can be made without major tool changes, i.e. change of mandrel or die only. On modern presses it does not take long to make a full tool change

(iii) composite products can be made

(iv) it is possible to make thinner tubes by extrusion than by other hot processes

(v) hollow shapes, including multi-hole ones, can be made

(vi) it is often possible to obtain a better yield by this process than it is by others when using materials difficult to hot-work

(vii) the products are metallurgically satisfactory in every way. The hot-working can be completed, practically speaking, at the same temperature

(viii) products can be made with a good surface finish and to fairly tight tolerances.

It may seem from what has been said that extrusion is a simple and straightforward process and that success can readily be achieved. It should be pointed out, however, that there are a great number of variables which must be continuously controlled in order to make good-quality products economically.

The cold extrusion of steel

R. A. P. Morgan, O.B.E., M.I.Mech.E.

SYNOPSIS

The cold extrusion of steel components was first carried out on a reasonably large scale by Germany during the Second World War. Its introduction was, to some extent, accidental insofar as a steel component was introduced into a die which was being used for non-ferrous component manufacture. The prospects of the process became immediately apparent but its success was also closely allied to the success of phosphating as a base for the lubrication film, the continuance of which during extrusion was essential for successful performance. The German authorities maintained the secrecy of the process during the war and it was after the termination of hostilities that details of the process were published.

The work at the Royal Ordnance Factory, Birtley, stemmed originally from a desire to examine the possibility of manufacturing shells by this process. It was found that there were certain limitations to existing designs as well as the physical characteristics required in the finished shell which made the proposal somewhat impracticable as a standard method of manufacture. Nevertheless, sufficient experience was gained to enable other component parts to be manufactured and the great benefits derived were: (a) economy in material, (b) improved physical characteristics, and (c) the knowledge that the material in the resultant component was physically sound. The knowledge of the cold extrusion process which was gained during this period made us feel that the process could, with advantage, be introduced into industry generally. It was also clear that the introduction of the process would make little headway unless private industry evinced an interest. To this end, therefore, development contracts for private industry were accepted in order to prove the process for certain items.

The Ordnance Factory at Birtley was particularly suited to engage in this field because of the wide scope of its press plant and its knowledge of cold drawing and general deformation possibilities of materials, and this paper brings out pertinently the advantages and disadvantages of the process known to date. 1767

INTRODUCTION

INTEREST in the cold extrusion of steel has increased of late to a degree where many drop stamping shops and manufacturers engaged in hot forging are anxious to know what prospects this new development holds for them and also what competition they may face from companies engaged in cold-flow forging.

The author's factory has been engaged for some years in developing cold extrusion practices and techniques to a degree which has unlocked some of the mysteries surrounding this method of manufacturing components, and as far as possible the process and its pitfalls are described in the paper in an endeavour to guide those entering this field. Considerable economy of material, together with increased strength characteristics, can be achieved by cold working and parts can be finish-tooled off the press. In large factories engaged in considerable machining from hot-forged blanks heavy expenditure is incurred in the manufacture of swarf and its removal from the works. This swarf is an encumbrance to any manufacturing establishment and considerable tool costs are incurred in its manufacture. A comparison of the cost of cold extrusion with standard machining methods, therefore, is not revealed by *pro rata* comparison of one method of manufacture against the other without including some factor for hidden economies, such as the reduction of swarf manufacture, which will also be achieved.

At first glance, cold extrusion appears to be very attractive and should merit serious consideration. There are factors, however, which militate seriously against its development in certain fields until the plant manufacturer has developed presses that are economically suitable for the process. For example, in connection with long, thin-walled, closed-ended cylinders, for safe and accurate working at the present time and with presses currently available, the cold extrusion expert is faced with the fact that punch loadings are so high that their length should be kept down to no more than three times their diameter, and cross-sectional area reductions of no more than 50% should be aspired to. It follows, therefore, that unless presses are designed that are automatic in sequence and will follow one extrusion operation by another, each doing a portion of the work, and finally following with one or more draw operations, cold extrusion of such components as specified cannot compete economically against high-speed machining of bar or tube stock. That is the position cold-extrusion specialists are facing today. The plant available has not been designed for this class of work and is therefore not economically suitable, but the day will certainly come when press manufacturers will design and market suitable presses in this country. Unfortunately in that respect we are lagging behind our competitors overseas, who are already meeting this requirement. Presses are available, however, for the manufacture of other types of component where the economies to be achieved are much greater. Such components as buffer cylinders, gear blanks, and the upsetting of bar stock can be economically achieved in a safe and certain manner,

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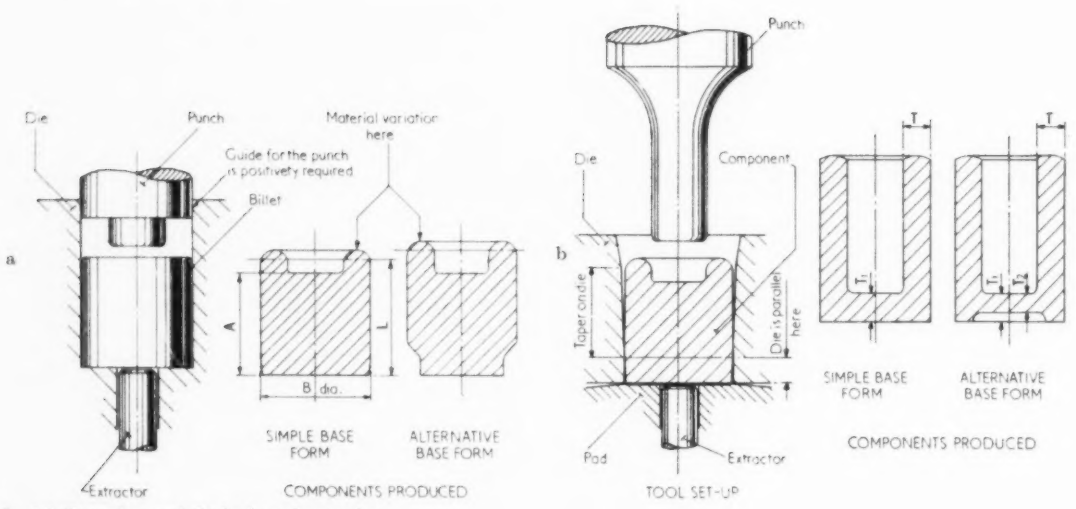


1 *(a) Dumping, and (b) backward extrusion*

provided that the designer is prepared to turn his attention in that direction.

Crude hot forgings requiring the removal of scale and considerable machining will undoubtedly give way to cold or warm extrusion, or a combination of both, with finished surfaces direct from the tooling employed, and considerable economy of material and labour can result.

PREPARATION FOR COLD EXTRUSION

Although it can be claimed, and indeed practised also in certain jobs, that more than one extrusion operation can be effected without an interstage anneal, it is not always practicable to do so without considerable 'down' time due to scoring or galling of the extrusion dies and, because of the considerable capital investment in the plant, every moment of active production time is valuable. In small plants it is, therefore, often normal practice to carry out an anneal, followed by a pickle, rinse, phosphate, rinse, and lubricate sequence of operations before each cold extrusion or cold draw.

These operations can be very expensive in their direct labour costs unless fully automatic plant, which can be bulk-loaded, is specially purchased. Although this means more capital investment, which has to be amortized or depreciated against the product cost, it is a wise investment in the long run in so far as the combined operation can be carried out relatively cheaply, as often as may be necessary. Moreover, the plant can be arranged to rock or tumble the components, which is very important if even phosphate and lubrication coatings are to be produced.

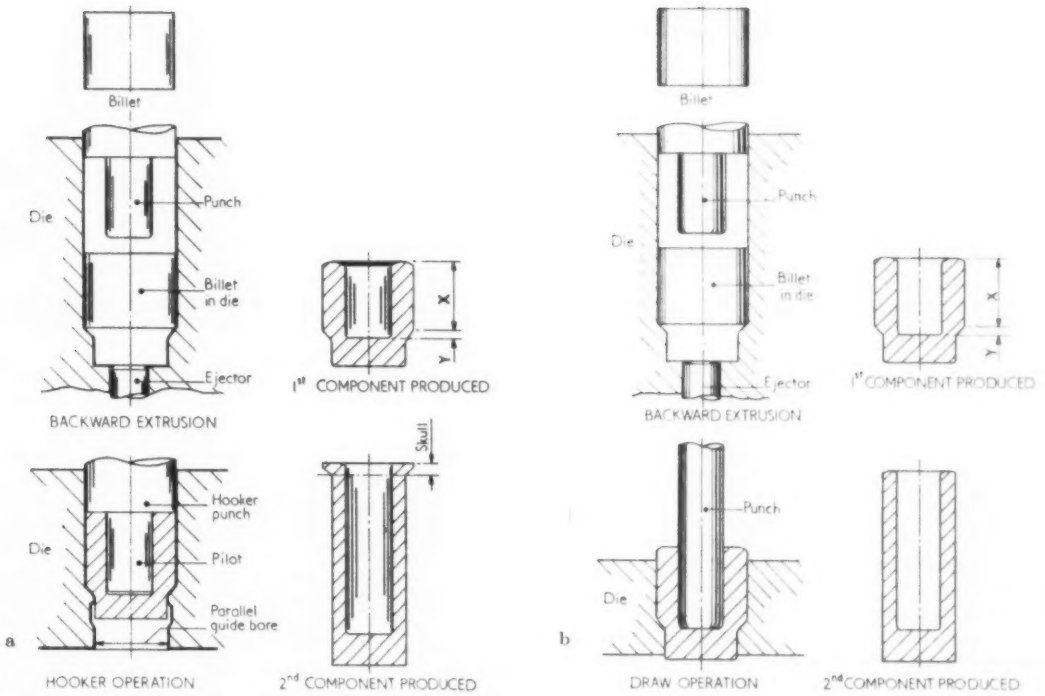
It is found to be an advantage generally to start cold extrusion from billets produced from hot-rolled bar. The reasons are as follows. First, the material is cheaper than turned or cold-rolled bar. Secondly, the material is already in a condition where it will withstand a certain amount of cold work without an annealing operation. Thirdly, the first degree of cold work placed upon the billet can be arranged to size it to the die for the first major extrusion operation. Fourthly, the cold work done on the billet in this condition makes the material more susceptible to easy spheroidization in the subsequent subcritical anneal and material properly spheroidized is considered to extrude better than if this condition is not obtained.

The first preparation for cold extrusion is obviously the cutting of the billet from bar stock. This can be achieved by sawing, shearing, or parting-off. All three operations can produce billets which are reasonably constant volumetrically and it is usually capital and existing plant conditions which determine the method to be followed. For automaticity, however, in feeding into the first press, it is obvious that billets must be free from rags. Also it is essential that billets are free from tears, which may represent unsoundness in the extruded component. In certain shearing operations undesirable tears result particularly when using hot-rolled bar stock. Sawing and parting-off operations always leave a rag or an upstanding centre pip on the end of the billet. Furthermore, if the sawing feed or the set of the blades is too coarse, objectionable markings on the billet surface results. Trials will establish the right sawing conditions and the operation can be quite cheap, as one operator can operate a number of saws. The rags and saw-cut markings can always be eliminated to present no problem at all, if the billets are properly rumbled before the first sizing operation. If the upstanding centre pip which occurs in parting-off is reasonably small, it does not present a problem in the extrusion operation as the centre portion of the billet surface does not laterally extrude very far. It can, however, be a nuisance in automatic feeding.

The first press operation

This can be called the dumping operation (Fig.1*a*). It follows the cutting and preparation of the billet and consists of squaring up the billet, dimpling, and increasing the outside diameter to a size to suit free access into the first extrusion die. As has been stated, this can be carried out without annealing and usually without lubrication on billets from hot-rolled bar stock.

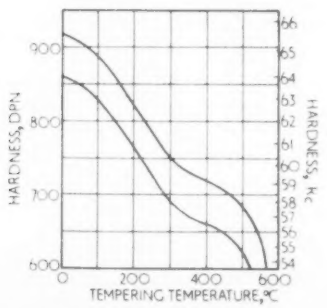
The operation is useful in that it takes account of the variation in bar diameter due to the hot-rolling



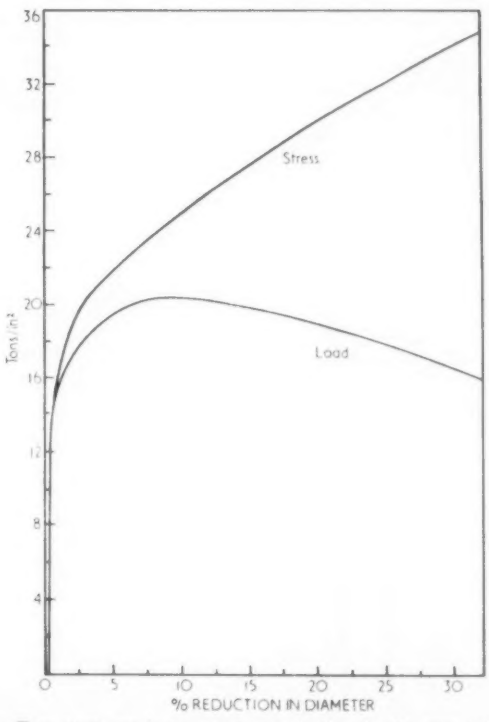
2 *(a) Method no.1 and (b) method no.2*

tolerance. It also corrects the inaccuracies inherent in sawing and shearing and establishes two dimensions *A* and *B*, as well as effecting a reasonable dimple in the top or, if the tools are reversed, the bottom face. At the same time the opposing faces of the billet are made parallel with each other and truly perpendicular to the die axis. The variation in material volume in the billets would be arranged to come at the top of the side walls and, if essential, components at this stage can be high-speed machined to length *L*. If necessary an alternative base form can be produced as shown which will probably assist at some later operation.

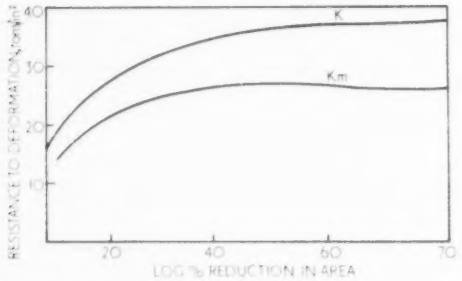
The prepared billet must now be annealed, pickled, phosphated, and lubricated before the main extrusion operation takes place and, as these processing operations are usually repeated without variation between any two extrusions or draw operations, a full description of the process is necessary.



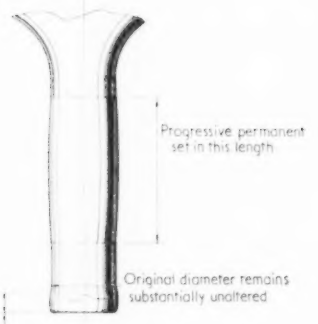
3 *Hardness/tempering temperature curves*



4 *True stress against percentage reduction in diameter (after Stead)*



5 *Values of k and k_m for 0·12%C steel*


Progressive permanent set in this length
Original diameter remains substantially unaltered
Permanent set and fine cracking occurs in this length

6 *Diagram of punch failure after constant use*

Annealing

This is usually carried out without the use of a protective atmosphere at a temperature just below the lower critical. Thus, for ordinary low-carbon steels the annealing temperature would be about 700°C. The annealing time can be important. On a billet of about 1¼ in. dia., ¾-h anneal is sufficient to recrystallize after cold work and to give a certain degree of spheroidization. Full spheroidization is not considered absolutely essential for steels of the 0·2%C range and below. However, if extrusion is to be carried out on a 0·3%C steel, greater spheroidization is considered necessary and an annealing time of up to 4 h may be considered necessary. It is important to achieve the greatest degree of softening and if it is convenient to leave a batch of work in the furnace overnight, furnace cooling should be resorted to. Quick cooling, of course, must always be avoided. Contolled atmospheres are obviously an advantage in reducing the amount of scaling that occurs but this is not essential and has never been carried out in the work done by the author to date. The hardness of the billet after annealing is normally about 120 DPN for a 0·2%C steel and 135 DPN for a 0·3%C steel, and this is considered suitable for normal extrusions.

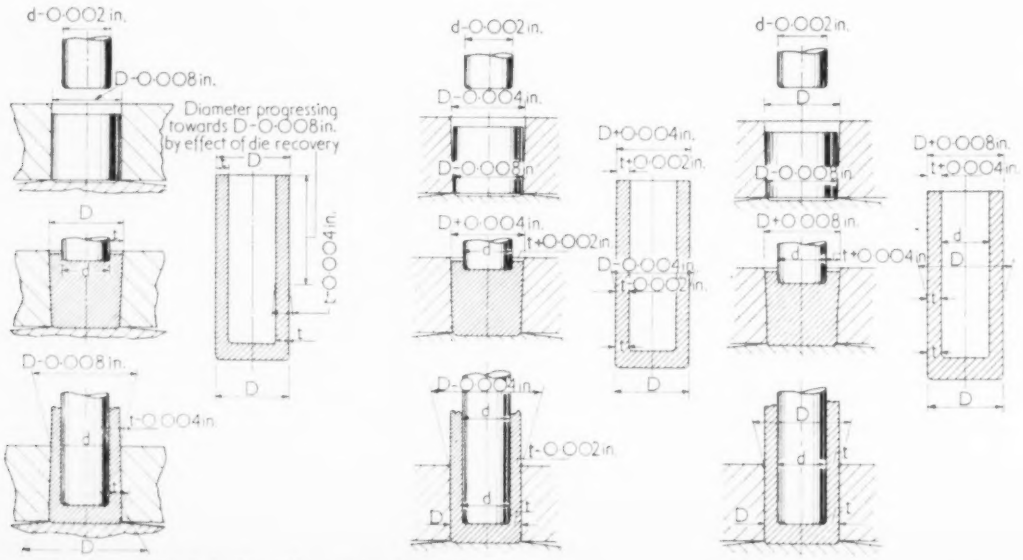
Pickling

This is usually carried out in a 10–15% vol/vol sulphuric acid solution with an inhibitor as a prevention against hydrogen embrittlement and also to obtain an evenly pickled surface. The inhibitor used by the author is Rodine III at a ½% wt/vol concentration. The temperature of the pickling solution is always 70°C and the time of immersion is normally 10 min. In the pickling and subsequent processing operations it is important to rock or tumble the components to reduce as far as possible the chance of certain areas of surface being improperly treated.

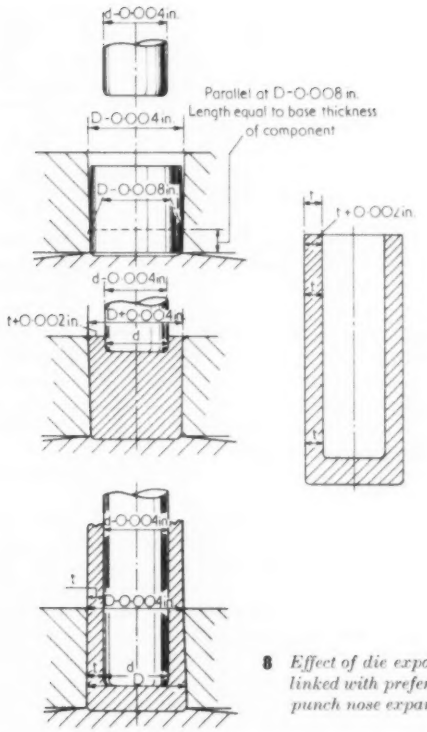
Rinse after pickling This is a cold running water rinse. Immersion time is usually 10 min.

Phosphating

It has been found in practice that a phosphate coating weight of about 2000 mg/ft² is adequate. German practice seems to indicate a lighter coating than this and there are other advocates of a heavier coating. Both practices have been tried out but without any improvement. Indeed, the lighter coating has been

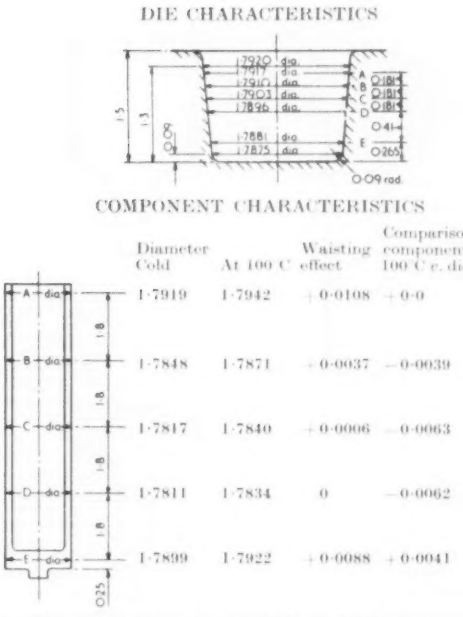


7 *Effect of die expansion linked with uniform expansion of the punch*



8 *Effect of die expansion linked with preferential punch nose expansion*

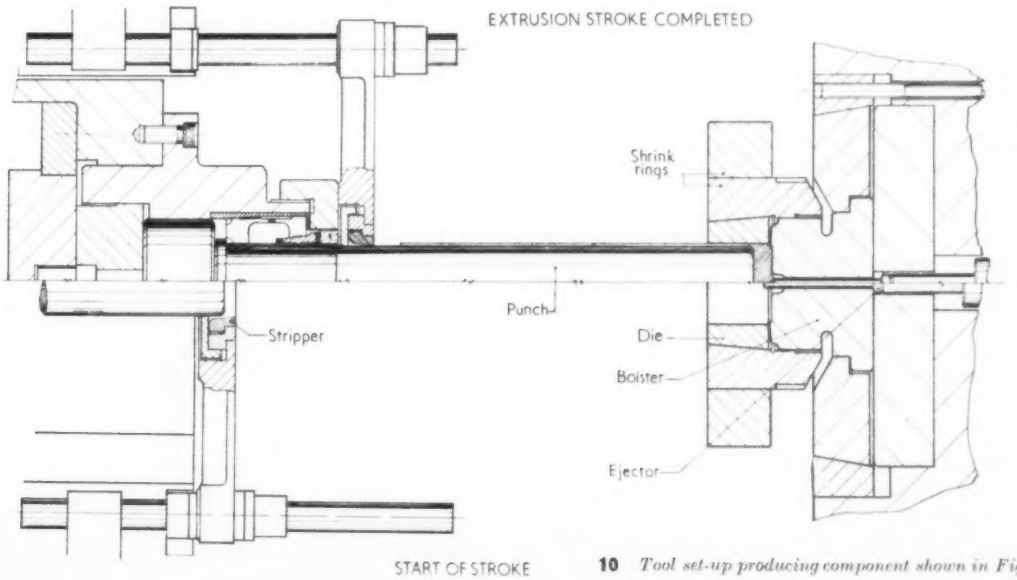
found to be detrimental, resulting in pickup and galling of the die. Coating weights can be increased up to 4500 mg/ft² by the addition of 5% wt/vol sodium nitrate to the pickling solution. In most of the work carried out by the author Bonderite 'D' series at 70 pointage and at 70°C has been employed. The immersion time is 10 min. The 'pointage' referred to is a measure of the free acidity and is measured by titrating against N/10 NaOH solution.



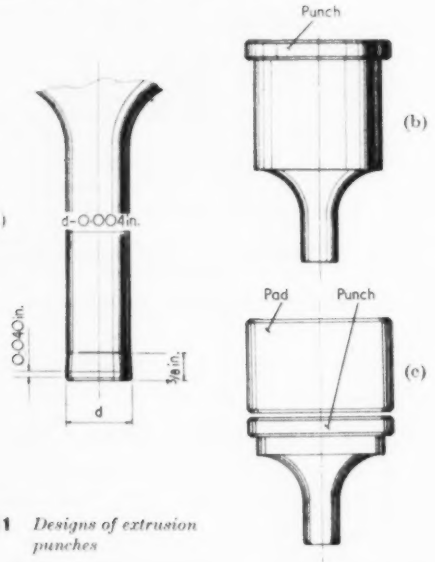
9 *Diagram showing the effect of the preferential expansion and contraction of the die as extrusion proceeds. Material extruded, 2·25%Mg–Al alloy; billet size, 1·785 in. dia. × 1·305 in. long; hardness before extrusion, 50 DPN*

Phosphating the component alters the size very slightly, and this may be of importance to the designer so far as access into the extrusion die is concerned. In phosphating, the parent metal is reduced in volume somewhat and this is replaced in thickness by a thicker coating of phosphate. As a result the diameter of the billet will be larger after phosphating by about 0·0015 in.

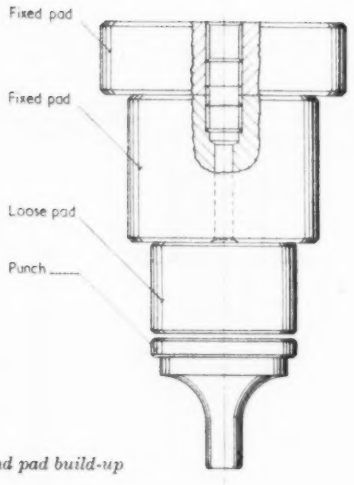
Rinsing after phosphating Two rinses are employed, the first a running cold-water rinse and the second a



10 *Tool set-up producing component shown in Fig.9*



11 *Designs of extrusion punches*



12 *Punch and pad build-up*

hot-water rinse to bring the billets back up to temperature for the subsequent lubrication. Time of immersion is conditioned by the plant cycle and in both cases is 10 min.

Lubrication

Proprietary lubricants advised by the manufacturers of the phosphating process used are considered best. Soap lubricants such as Artic Soap made by Palmolive Peet are also suitable but there is difficulty in controlling the coating weight and too thick a coating often results in fouling of the die in any automatic feeding arrangement. Because of this, the author has carried out most development work using Bonderlube 235 in a 12% wt/vol concentration. The Walterisation DX lubricant associated with the DX phosphating process has, however, been used with equal results.

The bath temperature is always 70°C and an immersion time of 5 min is essential to give time for the lubricant to react with the phosphate coating. It is important to ensure an even and thin coating of lubricant which dries off without tears or runs after removal from the tank. Failure to obtain this condition will certainly lead to extrusion difficulties.

Although simple lubrication as explained so far has been found to be satisfactory, an improvement in the component surface has been found to result when the billets are also dusted with a high-pressure powdered lubricant such as Abril 10 D.S. Slight evidence of stretched metal on the inner surface of the extrusion has disappeared and as a result more continuous production runs are experienced. Dusting with such a powdered lubricant, however, introduces complications first in the application, and secondly in the loading of the die which is caused by a coating of this lubricant packing solidly on the die bottom.

Second press operation, backward extrusion

This follows as the next press operation after dumping (*see* Fig.1*b*). The degree of cross-sectional area reduction is important in the maintenance of continued runs. The most heavily loaded tool is the punch and with a 50% cross-sectional area reduction, punch loading is of the order of 120 tons/in² (plus). Loading curves show that the extrusion pressure is almost immediately employed, which slightly increases as extrusion proceeds. The extrusion should not proceed beyond a base thickness ($T1$) which is equal to the wall thickness (T), otherwise shear effects occur in the side-wall–base junction and punch loading increases considerably. However, a thinner base can be achieved over part of the base area by arranging for an upstand in the die-pad.

As extrusion load comes on to the tooling the effect is as follows. The punch swells slightly and evidence seems to indicate that more swell occurs at the first ¼ in. of length from the nose. The die expands elastically at the mouth and as extrusion proceeds the mouth diameter tends to recover its original diameter while the die is forced to expand lower down. If this effect is considered at near the end of extrusion it will be observed that with a parallel die the wall thickness of the extruded component near the base is greater than the annulus between the recovered mouth of the die and the punch shank. This gives a choke effect which increases the load on the punch and is therefore

13 *Design of extractor–bolster*

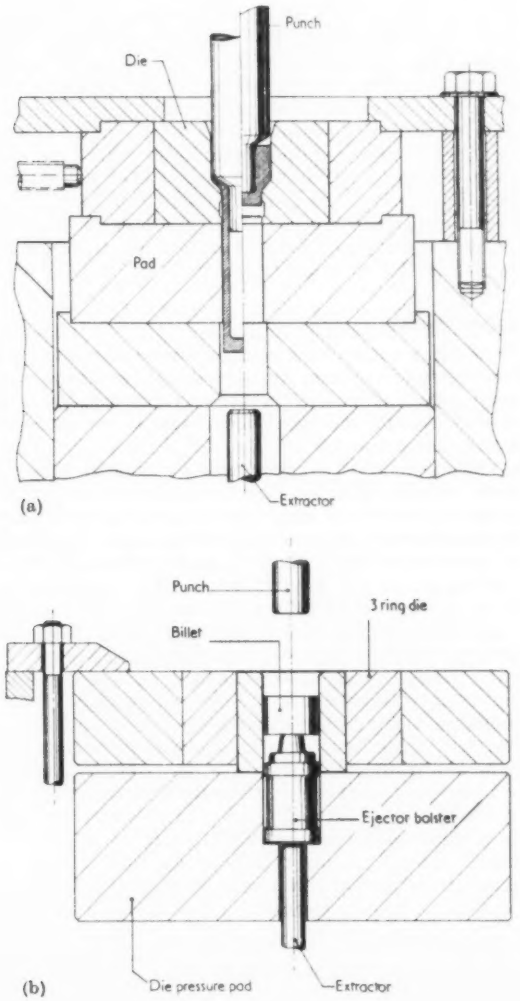
to be avoided, if possible, by tapering the die, allowing the mouth diameter to be larger than the base diameter. Because of this effect it is unlikely that perfectly parallel components are produced. It is seen that the die expands under load and the component produced is therefore larger in diameter than the die at rest. As soon as the extrusion load is released, therefore, the component is firmly held in the die. The punch recovers also and is usually free to withdraw from the cavity. The component is therefore left in the die and has to be extracted by means of an extraction bolster operating through the pad.

The length of the punch is very important in all backward extrusion operations owing to the column effect, which causes the punch to bend. If the punch is not centrally disposed and as near normal to the axis of the die as ingenuity can devise, the bending effect is more pronounced. For this reason, therefore, the effective punch length causing extrusion should not be greater than 2½–3 times the diameter of the punch. It is claimed by some experts that central guiding of the punch by means of a guide collar which is a very close fit on the punch, the working allowance being of the order of 0·0002–0·0007 in., permits accurate backward extrusion up to five times the diameter. The experience of the author so far is that the mechanics of locating the loose collar with the die to maintain the absolute exactitude of location that is required makes this arrangement very difficult and tooling up becomes very expensive, so that before embarking on such a proposal this aspect requires very close examination.

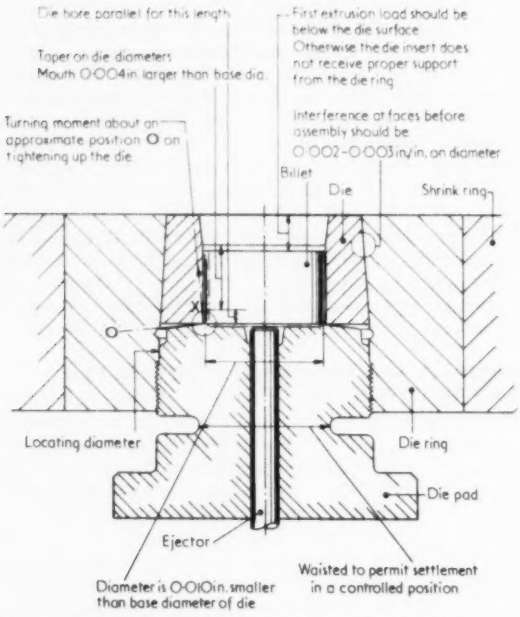
Feeding and extraction should be automatic and the components collected in a workbox for a subsequent anneal, pickle, phosphate, and lubricate sequence, and a further operation, which can be either a draw or a forward (hooker) extrusion.

ACCURACY AND THE CHOICE OF METHOD IN A TWO-OPERATION JOB

It is important to remember that it is frequently impossible to cold-extrude a steel component in one operation and when the economics of the operation are compared with the cost of high-speed machining operations, where an 80–85% running time efficiency can be expected, there is often very little to spare in favour of cold extrusion. To be an economic success, therefore, it is essential for all interstage operational processes to be carried out at high speeds and this demands complete reproducibility of component and complete accuracy of relationship between one opera-



14 *(a) Typical hooker extrusion, (b) typical backward extrusion, and (c) typical forward extrusion*


Die bore parallel for this length
Taper on die diameters
Mouth 0·004in. larger than base dia.
First extrusion load should be below the die surface. Otherwise the die insert does not receive proper support from the die ring
Turning moment about an approximate position O on tightening up the die
Interference at faces before assembly should be 0·002–0·003in./in. on diameter
Billet
Die
Shrink ring
O
X
Locating diameter
Die ring
Die pad
Ejector
Diameter is 0·010in. smaller than base diameter of die
Waisted to permit settlement in a controlled position

15 *Die design for expansion and settlement under load*

tion and another. A simple example to consider is that of a long closed-ended cylinder which is too long to extrude in one operation, since a long punch will tend either to wander or to bend. After the preliminary dumping, which would be carried out as already described, such a component would possibly be produced in two main press operations, namely, either method 1, Fig.2*a* (*a*) a backward extrusion operation followed by, (*b*) a forward (hooker) extrusion operation, or method 2, Fig.2*b* (*a*) a backward extrusion operation followed by (*b*) a draw operation.

At first glance method 2 would be favoured because the draw operation succeeds in drawing all the metal into the wall, whereas the hooker operation always leaves a skull of metal that has to be wasted, but this may not be practicable. The two methods referred to are illustrated in the accompanying figures and, unless there is a continuance of the initial and complete accuracy demanded from the first setting, very inaccurate components will be produced which cannot be salvaged.

Accuracy must therefore be insisted upon at all stages from the preparation of the billet, which must be volumetrically reproducible within very close limits, and through all interstage operations.

Such accuracy as is required, however, can never be obtained if the press is not accurate in itself under extrusion loads. Experience to date leads to a very serious doubt as to the accuracy of presses even under static conditions and has made it necessary to line up presses to as near perfection as possible before permitting any tools to be mounted. Any inaccuracy in alignment of punch with die will cause a punch hardened up to 65–67 R_C to shatter and this effect may force designers to use a softer punch with consequent lower tool life. If the backward extrusion punch is not accurately centred in the die, the wall thickness will not be constant and this cannot be corrected by the subsequent draw or hooker operation. If the depth of backward extrusion varies, the proper location of the component produced in relationship to the punch and die is not maintained in the subsequent operation and this very often results in undesirable effects such as overstrain on the metal. It must be accepted that billets cannot always be exactly the same volumetrically and allowance must be made for this in the design of the first operation. A decision has to be made as to whether the top or the bottom of the component produced in this operation may be allowed to vary. It is usually found that the dimensions X–Y are very important and should be maintained. A decision has also to be made as to whether the annular surface on the top of the component has to be accurately tooled or the thickness through the base. If method 2 is used, it would appear that the base thickness would be most important, as the operation of backward extrusion can eliminate any further machining of the base. After the draw operation it then becomes necessary only to trim the mouth to a specified length. If method 1 is used, however, it is important to tool accurately the annular surface at the top of the component. If this is not done the hooker operation which follows the backward extrusion will not be balanced and the component will be bent notwithstanding the parallel guide-bore that follows the extrusion die. Considerable accuracy in the formation of the working surface of the extrusion die for a hooker operation is also required if this undesirable effect of bent components is to be avoided. These undesirable effects do not occur to anything like the same degree in a draw operation as the inaccuracies are allowed to go free into the side-wall length. For this reason, as well as the fact that a draw-die is much simpler and cheaper to produce, a draw operation as in method 2 is normally favoured.

PLANT COSTS

In carrying out an assessment of the economics of the cold extrusion process the question of plant costs is a very important factor. Special consideration must be given to the press design, which must be robust to an unusual degree.

Once purchased, a press must be capable of being used on many different types of operation and so the press characteristics must be sized on a generous scale as regards both physical size and tonnage. The speed of operation is very important as it is obviously more economical to pay £40000 for a press operating at 15 strokes/min than £30000 for a press operating at 8 strokes/min. Presses are, in any case, so expensive in first costs, that very large outputs are required in order to have a reasonable amortization. A press working at 15 strokes/min, however, will produce 31680 components in a 44-h week if working at 80% efficiency and few firms have orders to place of this magnitude. If a 15 strokes/min press is purchased, therefore, complete sub-press tool sets should be designed which can be removed and replaced by another such tool set for a different component inside an hour. It may be argued that the figure of £40000 for such a press is higher than present-day prices. However, the following typical characteristics should be borne in mind:

Daylight 5 ft 0 in. (stroke down)
Bed area 5 ft 0 in. × 4 ft 6 in.
Tonnage 500

TABLE I Percentage composition of steels

	C	Mn	Si	Ni	Cr	Mo
0·08%C fully killed steel	0·08	0·12	0·07	...	...	...
Commercial mild steel En 3.4	0·15–0·25	0·40–0·90	0·05–0·35	...	...	...
Mn–Mo steel En 17	0·30–0·40	1·30–1·80	0·10–0·40	...	...	0·35–0·55

Deflection of bed under maximum central load limited to 0·002 in.
Stroke, 20 in.

Bottom extraction	*Top extraction*
Stroke, 12 in. minimum	Stroke, 8 in.
Power, 100 tons	Power, 50 tons

A mechanical press of this type should be of the double-geared eccentric-operated type. The ram should be of very deep construction and very accurate guiding must be a feature. Lineability of the ram platen with the press bed should be very accurate and no more than 0·001 in. out of parallelism over the whole bed area should be allowed. With a press to these characteristics a sub-tool press set with automatic indexing can be designed which will fit accurately and easily into position permitting quick changing of tool sets.

Such a sub-press tool set incorporating six stations in itself would cost about £4000 and such a tool set could be expected to carry out all cold-working operations required, including drawing to an approximate finished length of 10 in. In a six-station automatic tool set, two feeding stations, two backward extrusions, and two draws could be accommodated so that a duplicate of a three-operational tool set could be provided and double the number of components produced per unit of time.

If all six stations are to be used to produce one component it is clear that four extrusion stations are being used. That being the case, very careful sequence of operations will have to be planned to take account of the maximum workability of the steel. In this connection, reference is made to the peculiarities of preferential extrusion of the material occurring in a simple backward extrusion from which it will be noted that the base surface and nearby areas extrude the least in this operation, whereas areas of the top surface which subsequently form the cavity extrude the most. It will be quite clear that unless the whole of the six stations are fully used there is wasted effort by the press. The accuracy of guiding required in the ram of such a press is obvious from the fact that six stations will be used and there will be some unequal loading and consequently preferential straining of the ram face.

The preference of mechanical and hydraulic press or vice versa is probably vested in the number of strokes/min and power considerations. A mechanical press obviously has the advantage in this direction. Nevertheless, hydraulic presses can be considered to be kinder to the tools and particularly the punches in the long run. The crank angle effect of a mechanical press after some time in use, when the gudgeon bushes and eccentric sheaves are showing signs of wear, can be disastrous to a punch which is already locked in a component in so far as the throwover of the ram against the gibs effects a bending movement on a punch locked at each end. For that reason, therefore, the adverse press effects that can be present in a mechanical press after some years of use should be eliminated by making the sub-tool press sets complete in themselves and most rigidly guided.

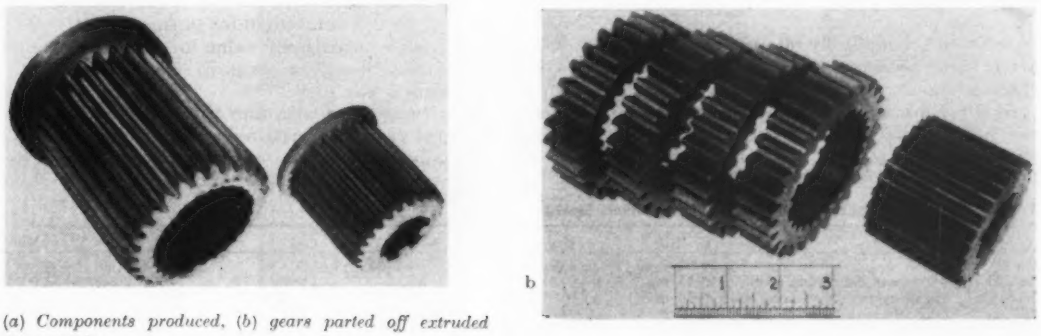
Having considered the main press at some length and discussed its cost of £40000 plus £4000 for tooling, attention can now be given to the other equipment required.

Total plant costs

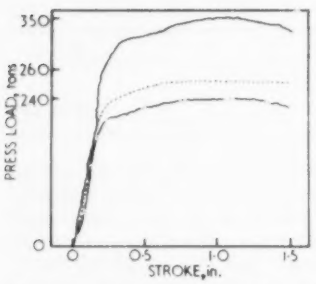
	£
12 saws or 1 shearing press	10000
4 rumblers	2000
1 furnace	18000
1 processing plant	10000
1 press for dumping with automatic feeding gear	12000
1 extrusion press	40000
1 sub-press tool set	4000
	96000

Saws Based upon 3–4 min operational time and 80% efficiency, 12 saws would be required, which would represent a capital investment of about £10000.

Rumblers About 4 rumblers valued at £2000 would be needed. The weight of material handled per hour assuming billets $1\frac{1}{2}$ in. dia. × $2\frac{1}{2}$ in. long is about 1820 lb.

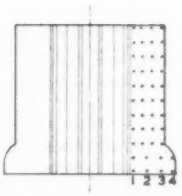


16 (*a*) *Components produced,* (*b*) *gears parted off extruded components*



17 *Stroke/load curves of the steels used in producing the components shown in Fig.16*

0·08%C steel				Mild steel				En 17 steel			
1	2	3	4	1	2	3	4	1	2	3	4
170	187	207	213	197	212	218	235	254	272	268	287
192	201	230	222	219	225	242	236	260	276	281	317
199	213	227	235	218	222	238	242	268	264	283	322
201	212	238	227	221	225	242	238	262	268	283	322
212	225	249	240	225	235	244	243	258	274	285	322
210	213	243	236	225	230	245	245	281	285	294	336
218	205	243	238	225	242	256	254	256	279	299	339
203	210	238	233	218	235	253	264	266	281	297	345
213	213	243	245	218	233	249	262	264	284	285	339
202	202	218	203	224	205	245	199	260	272	288	228
216	188	221	198	233	182	230	215	270	232	262	233



18 *Section hardness figures*

Furnace A suitable furnace to anneal these up to a maximum of 750°C would cost about £18000.

Pickling, rinsing, phosphating, and lubricating plant An automatic plant for this, incorporating tumbling barrels, would cost about £70000.
Plant amortized over eight years at 60% efficiency, 48-week year, works out at 3·10*d* per component.

PUNCH MATERIAL

Realizing that the success of any cold-extrusion process lies very largely in the life of the punch, the first material used by the author was high-speed steel because of the high compressive loads it can be expected to withstand. It was found, however, that punches in HSS could not be relied upon and they frequently shattered. It is not certain at this stage how much this adverse affect was due to inaccurate lineability as since that time various inaccuracies have been found in the presses and in large measure these have now been corrected. Even so, failures still occur in the early life of the punch which have been attributed mostly to inaccurate setting-up in the first trials. As a result it is now standard practice to use setting masters as a gauge to the accuracy of initial tool setting. The failure of HSS punches, however, turned attention to the use of alternative steels and production continued with the use of 2%C–12%Cr steel, the characteristics of which are given below. Punch life, however, is still variable although much longer runs of up to 8000 components/punch were being obtained.

Nominal composition, 2%C–13%Cr. Otherelements are not normally specified or published but would be in the order of 1% each of silicon, manganese, and nickel. Sulphur and phosphorus would be roughly 0·030% each.

Hardening Usually by oil quenching from 950–980°C after slow preheating first to 700–750°C to avoid thermal shock.

Small sections will air harden (up to about 1 in. dia.) but in this case it is usual to increase the hardening temperature to about 1000°C.

Water or brine quenching would result in:
(i) severe cracking
(ii) a marked reduction in hardness due to retained austenite. Some of the austenite would be transformed on tempering but the overall effect would be to give a very much flatter curve than that below but at a lower level starting at 50–55R_C in the hardened condition.

Tempering Hardness/tempering-temperature relationships are given in Fig.3, which shows the range of hardness to be expected. Temper to obtain a hardness of 750 DPN.

In the meantime certain high-silicon steels were produced and the first punch in this type of steel yielded 12000 components before the punch failed owing to wear at the nose. Meantime Cu–Si–Mo–V steels have been developed which show more interesting characteristics and appear to be superior to the high-silicon steels. It is proposed to continue developments by trying out these steels as well.

Attention has also been given to the manufacture of tungsten carbide punches and certain designs are in the process of manufacture. Very considerable benefit can be derived from the successful use of tungsten carbide not only because of the increased wear characteristics but also because its Young's modulus is so much higher. Longer extrusions should be possible because the column effect upon the punch will be reduced. These developments have not so far been concluded.

THEORETICAL APPROACH TO PUNCH LOADING

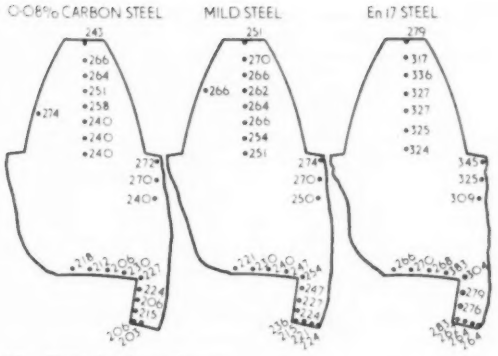
In the deforming of any plastic material by rolling, drawing, pressing, extruding, etc., the work done and therefore the force applied always depends on:
(i) the volume of the material being deformed, V
(ii) the resistance of the material to deformation, k
(iii) the greatest change in dimension in any of the three principal planes, ϕ sometimes quoted as the deformation index or ratio and always given as a logarithmic value in order that the rate of change will be constant.

The value ϕ

It is practical to assume that during extrusion the volume of the material remains constant. Therefore, if we take an elemental cube of material whose dimen-

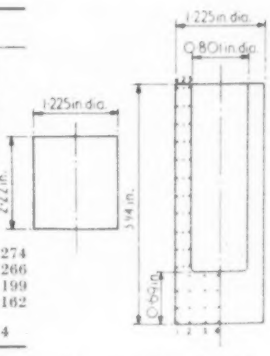
TABLE II Percentage compositions of other steels tested

	C	Mn	Si	Ni	Cr	Mo
En 18 1%Cr steel	0·35–0·45	0·60–0·95	0·10–0·35	...	0·85–1·15	...
En 361 low-alloy case-hardening steel	0·13–0·17	0·70–1·00	0·35	0·40–0·70	0·55–0·80	0·08–0·15
SAE 8615 low-alloy case-hardening steel	0·13–0·18	0·70–0·90	0·20–0·35	0·40–0·70	0·40–0·60	0·15–0·25
S 82 4½% Ni–Cr–Mo case-hardening steel	0·12–0·18	0·50	0·10–0·35	4·00–4·50	1·00–1·40	0·15–0·35
S106 nitriding steel	0·20–0·25	0·10–0·35	0·40–0·65	0·30 max	2·90–3·50	0·40–0·70
S107 3%Ni–Cr–Mo case-hardening steel	0·18	0·10–0·35	0·30–0·60	3·00–3·75	0·60–1·10	0·10–0·25



19 *Tooth hardness figures*

0·08%C				Mild steel			
1	2	3		1	2	3	
167	167	163		196	206	222	
199	209	233		219	227	238	
205	222	253		240	236	258	
222	227	266		219	236	253	
206	224	264		232	240	260	
215	225	270		224	236	258	
216	227	262		230	240	264	
216	219	266		233	236	253	
221	225	274		226	238	260	
225	222	276		228	236	268	
218	218	258		232	245	270	
	309	268	272		322	268	274
209	222	262	253	224	254	266	266
151	169	191	185	161	193	196	199
133	128	152	151	167	153	168	162
1	2	3	4	1	2	3	4



20 *Proportions and hardness figures of component produced by single impact*

sions in the three principal directions are x, y, and z before extrusion and x_1, y_1, z_1 after extrusion, we can say

Volume $V = xyz = x_1y_1z_1$
or simply $xyz = x_1y_1z_1$

Thus taking logarithms

$$\log_e x/x_1 + \log_e y/y_1 + \log_e z/z_1 = 0$$

and $$\log_e x/x_1 = \log_e y/y_1 + \log_e z/z_1 \quad \text{.....(1)}$$

In the above the signs + and − have been ignored as this theory relates to deformation (i.e. change in dimensions) and it does not matter whether the material lengthens or shortens.

In equation (1) the log deformation ratio in one direction (x) is equal to the sum of the log deformation ratios in the other two directions and therefore $\log_e x/x_1 = \phi$. If we deal with cylindrical components as we mostly do in steel extrusion, this log deformation ratio $\log_e x/x_1$ (which is ϕ the greatest dimension change) can be related to the change in cross-sectional area = A/a. Where A = area of billet and a = area of component after extrusion. Therefore ϕ (the deformation ratio) = $\log_e A/a$.

The value k

This value depends on the chemical composition of the material and it increases as the carbon content and/or alloy content of the steel increases. It also depends on the grain structure. The fully annealed spheroidized structure being the least resistant, the value of k increases as the degree of cold working increases. It will be seen, therefore, that k is not constant for a given material but can vary with its condition and the amount of cold working (or strain) it is subjected to.

This fact may be established by considering the true stress/strain diagram taken from results of a tensile test. The diagram shown in Fig.4 is after Stead. It will be noted that after about 8% reduction in diameter, the true stress curve continues to rise rapidly. This is because although the load curve is falling, the area of the test-piece is rapidly decreasing owing to 'waisting'.

It will be appreciated that k can vary during the cold-forming operation and most authorities work with a mean resistance to cover this, generally denoted by k_m.

Fischer in his paper to the Sheet and Strip Metal Users Technical Association at their conference in 1953 gave curves showing the relationship between the percentage reduction in area and the two values k and k_m for a 0·12%C steel; Fig.5 is taken from it.

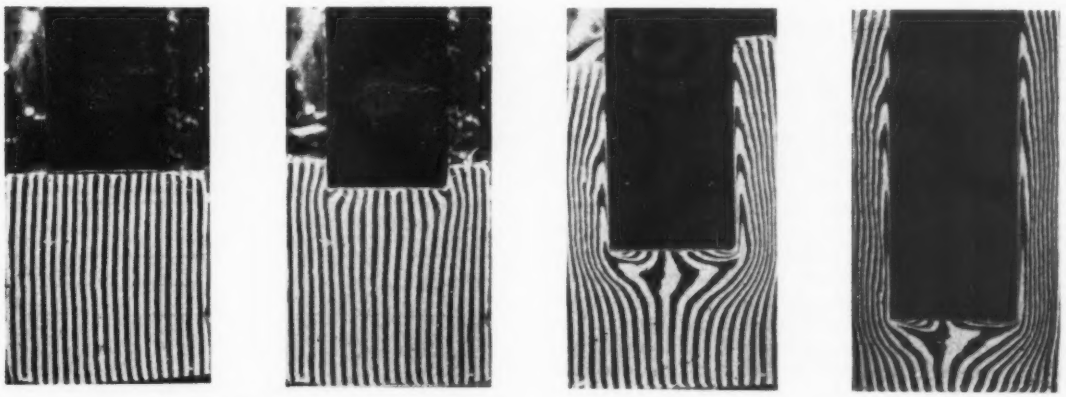
We can now go back to the first statements and write an equation (theoretical) for cold work:

work done = volume × resistance of material to deformation × the log deformation ratio.

or work done = $V \times k \times \log_e A/a$

$\therefore$ pressure required for extrusion = $k \log_e A/a$.

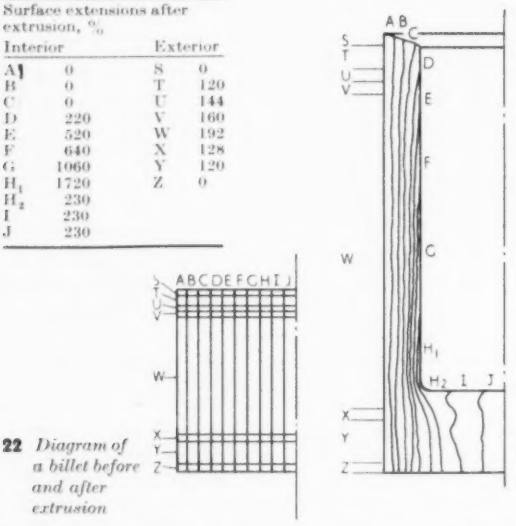
This theoretical statement does not take into account the work lost (or the efficiency of extrusion) in overcoming friction between material and tools, internal



21 *Four stages in the extrusion of a laminated Plasticene billet*

Surface extensions after extrusion, %

Interior		Exterior	
A	0	S	0
B	0	T	120
C	0	U	144
D	220	V	160
E	520	W	192
F	640	X	128
G	1060	Y	120
H_1	1720	Z	0
H_2	230		
I	230		
J	230		



22 *Diagram of a billet before and after extrusion*

shear in the material itself, and work done in deforming tools. Therefore a constant must be added to allow for these losses, say $1/N$.

$\therefore$ Pressure required for extrusion $= k \log_e A/aN$

This efficiency ratio has been experimentally determined and for backward extrusion is approximately $N = 0{\cdot}3$.

Therefore dealing with a backward extrusion in 0·12%C steel of about 60% reduction in area we get

pressure for extrusion, tons/in² $\quad k = 39$, $k_m = 27$

$= 39$ tons/in² $\times$ 1·1 $\times$ 1/0·3

$= 143$ tons/in² (or 100 tons/in² taking k_m).

For forward extrusion the pressure required is lower, probably owing to the change in external friction conditions and therefore a higher efficiency ratio.

TOOL DESIGN

The high loads and stresses imposed upon cold-extrusion tooling are such that unless special measures are taken the tools will deform plastically or fracture. The tool that has to support the whole of the extrusion load by the shear merit of its strength associated with some ductility is the punch. Its design and manufacture must, therefore, be as perfect as possible. The other tools, namely the die and the die pad, can always be suitably supported to such an extent that the stresses imposed can always be within the elastic range. It must be borne in mind, however, that all tools will elastically strain under load and will therefore produce a component of different size to that given by the tools at rest. A steel punch made of a 2%C–12%Cr steel hardened and tempered to about 740 DPN expands under load to a degree dependant upon its diameter. The effect of the extrusion pressure upon such a punch must be considered from two aspects, namely as new and under first trial, and also after having been in use for a considerable run. When first used, the punch being new will strain elastically. Evidence seems to point to the fact that diametral strain occurs in two places, namely at the nose and at a position on the shank from the one-third to one-half

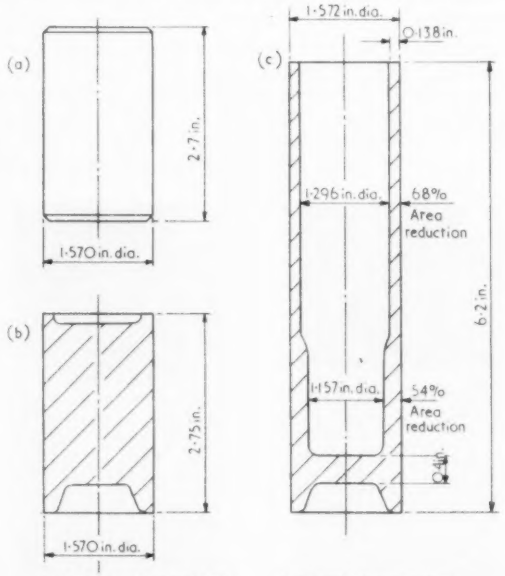
TABLE III Mechanical properties of steels before and after extrusion

	YS, tons/in²	UTS, tons/in²	Elong. $4\sqrt{A}$, %	R in A, %	Izod, ft/lb	Hardness, DPN
Low-carbon steel						
Annealed	14·1	24·3	45	65	80	108
Extruded	48·3	49·3	24	46	28	...
Extruded	50·0	50·8	16	46	45	...
Mild steel						
Annealed	15·2	25·2	40·5	68	78	122
Extruded	51·8	52·7	23	45	29	...
Extruded	52·8	53·0	23	44	48	...

length, upwards. Unless some allowance is made for this in the design of the punch, the effect of an expansion in diameter of the punch from one-third to one-half length position upwards will be to work in concert with the recovery of the die mouth in causing a choke effect at the annulus between the punch and the die mouth, which will increase the frictional resistance.

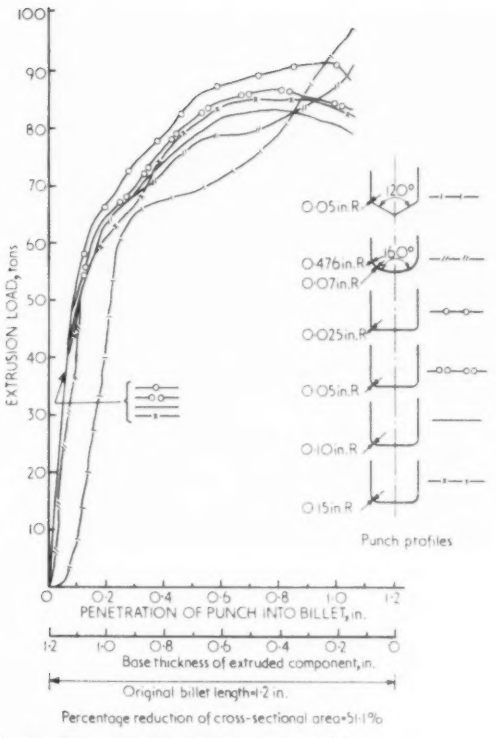
After a punch has been in use for some time a degree of permanent set occurs and punches removed from service after failure are found to be as shown in Fig.6.

A die is usually made so that it rests on and is attached to the die pad. Dies for the cold extrusion of steel are now made of three rings, the inner ring being the die proper and the two outer rings being shrink rings which impose a compressive hoop stress in the inner ring. By this means the stresses through the die are evened out as far as possible during the extrusion stroke and the tensile hoop stress on the bore of the dies kept down to sustainable limits. In cold extrusion of steel, punch pressures of the order of 120 tons/in² are produced. If this is considered as a fluid pressure, a radial pressure on the inner face of the die of similar order can be expected. In an unsupported die such radial pressure can produce a tensile hoop stress at the die-bore of approximately 136 tons/in². To counteract this it is normal practice to induce into the die bore a compressive hoop stress of about 70 tons/in² by means



(*a*) billet (*b*) sized billet (*c*) extruded component

23 *Single-shot extrusion of a shell body*



24 *Load diagrams for various punch profiles*

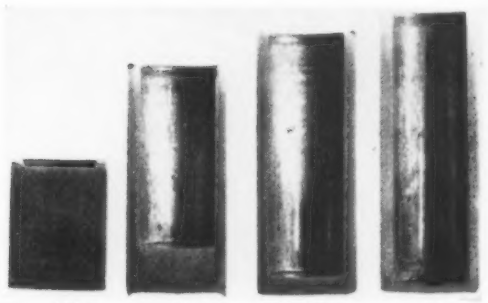
of the two shrink rings each imposing 35 tons/in². This would reduce the tensile hoop stress under load to something of the order of 66 tons/in². If a tungsten carbide die were used, however, no tension hoop stress under extrusion load can be allowed, otherwise the die would crack. In such case a compressive hoop stress equal and opposite to the full tension hoop stress obtained under the extrusion load would have to be applied and shrink rings of high-quality steel would, therefore, be required.

At first extrusion impact, the mouth of the die is forced to expand and, depending upon the dimensional aspects, the conformity and the method of securement of the die, the expansion at the mouth may, on first impact, be greater than that at the base diameter. However, as extrusion proceeds the effect is to make the die diameter expand progressively downwards through its depth, whilst the mouth of the die attempts to some degree to recover its original diameter. Whilst this is occurring, the upper portion of the punch shank and the nose of the punch are increased in diameter under load and as a consequence of the choking effect

TABLE IV Warm temperature effects

Temp.	Test sample	Peak load on punch, tons/in²	% decrease in load from *D*1
Ambient	*A*1	150*	...
	*A*2	154*	...
100°C	*B*1	146	16
	*B*2	139	20
200°C	*C*1	106	39
	*C*2	112	36
Ambient	*D*1	175†	...

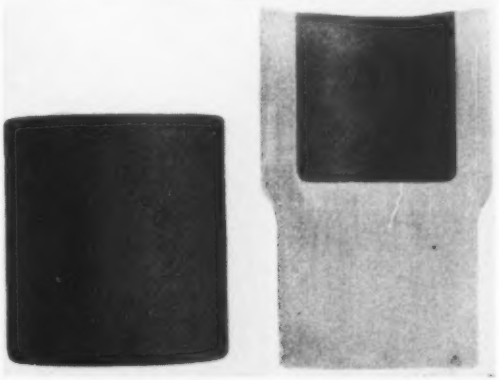
*Test stopped before peak load on reaching safety limit of tools
†Peak load, but bolster fractured immediately afterwards



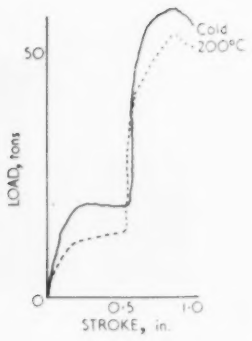
25 *Components used in testing warm temperature effects*

at the die mouth frictional resistance to upwards flow becomes very high. The effect of the expansion and recovery of a die is shown in Figs.7*a–c*, and 8, but is perhaps best illustrated in the production of a high-strength aluminium alloy component made on a Herlan press. This component (*see* Fig.9) was produced from a tool set-up as in Fig.10. The interesting facts about this component is that it is waisted in the centre and it is clear from deduction that at the start of extrusion the die expanded at the mouth more than it did at the base. An analysis of the tool set-up shows this to be quite possible. An extrusion lip on the punch 0·024 in. greater in diameter than the punch shank permitted full recovery of the die mouth as extrusion proceeded.

Design of tools for steel extrusion cannot be successful unless the effects upon them of the extrusion load are carefully considered and understood. In considering the punch the length must be reduced as much as possible to reduce undesirable bending effects. The formation of an extrusion lip may well cause the component to be extracted on the punch and this would then require the punch to be lengthened in order to fit a stripper. Whereas, therefore, an extrusion lip on the punch may well reduce the extrusion load it may not be practicable to allow its use, as the punch length may be increased above the safe margin of $2\frac{1}{2}$–3 times its diameter. If an extrusion lip is considered essential, its proportions should be somewhat as shown by Fig.11*a*. Bearing in mind that the punch must seat



26 *Forward and backward extrusion using a production set-up*



27 *Load/stroke curves of component shown in Fig.26*

TABLE V

Temp.	Actual tool loading, tons			
	1st peak	% decrease from cold	2nd peak	% decrease from cold
Ambient	17·4	...	57·0	...
	18·6	...	57·0	...
150°C	12·4	31	51·0	10
	13·6	25	51·0	10
200°C	12·4	31	52·0	9
	12·4	31	52·6	8
250°C	12·4	31	53·5	6
	14·2	21	54·5	4

against a pressure pad the designer may effect a design as in Fig.11*b*. This, however, is not good from the heat-treatment point of view as well as being expensive from the replacement point of view, and Fig.11*c* represents a better design in regard to these aspects.

Except for the fact that the greater the number of parts in any set-up, the more risk there is of dimensional and flatness inaccuracies, a build-up of pads as in Fig.12, bringing the eventual load evenly over the press platen, is preferred to solid tooling from both the economy and the heat-treatment points of view. The flatness and parallelism of the pads is particularly important; all should be surface-ground.

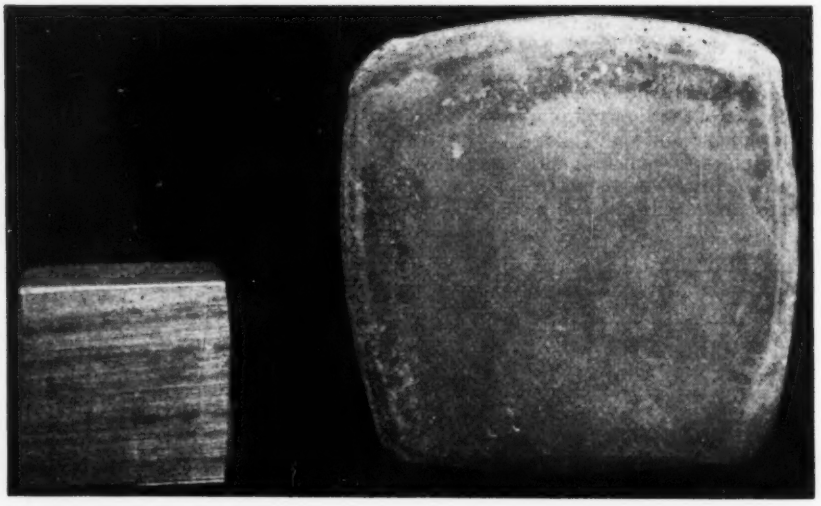
The punch nose surface is preferred to be flat, finishing with a small radius to form the side wall of the punch. The size of the radius is important as it has some effect upon the pressures required for extrusion. The flat surface of the punch serves to maintain the phosphate and the lubrication without fracture and this keeps the punch loading down. Once the lubrication surface is broken, the punch loadings required rise steeply and the graphical results showing the effect of a conical surface on the punch illustrate this very well.

In the heat-treatment of punches, great attention must be paid to the surface before quenching. It is important to remember that there must be no stress raisers, and a ground surface before heat-treatment is desired. Punches should be packed in charcoal whilst in the furnace to limit preferential heating or cooling as far as possible and should be quenched out from the nose upwards.

The bolster should be treated in exactly the same way from the design and manufacturing standpoint as the punch. If, however, the bolster is to be used also as an extractor, the design should be such as to enable it to expand under load in a preferred position as shown in Fig.13.

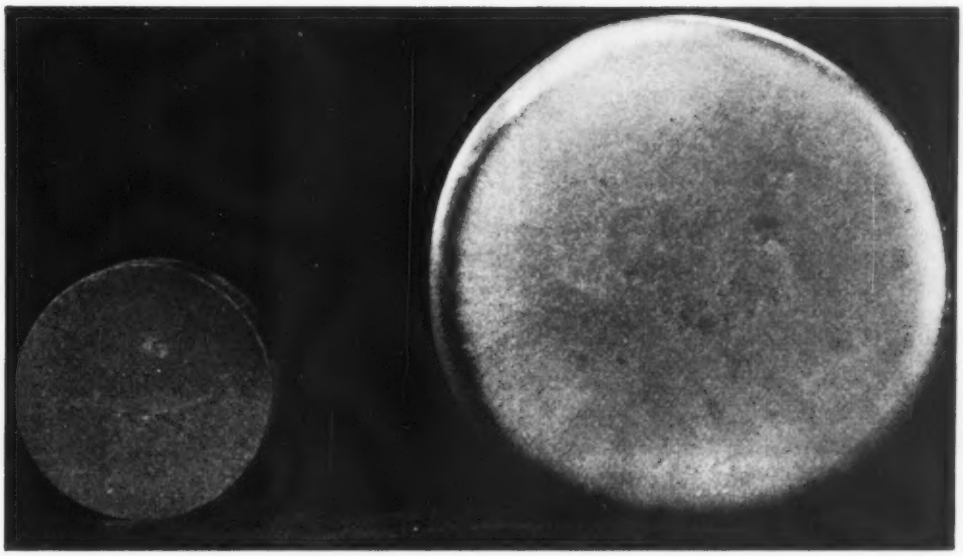
In considering the die design, Figs.14*a–c* and 15 show different set-ups which have been found to be satisfactory. In Fig.15, troubles have occurred in the maintenance of a tight joint between the die and the die pad which were overcome by allowing the die pad to set down elastically under load in the position shown. When this die is securely screwed on to the die pad there is a turning movement imposed upon the die which tends to open the mouth. In a properly stressed die this effect would be small, but nevertheless the mouth diameter of the die should be checked before and after assembly.

The most important part of die design lies in the accurate stressing and proportioning of the die proper and the shrink rings. Experience has shown that all shrink rings and the die proper have an internal/external diameter ratio of 1 : 1·5 and that there should be a shrink allowance of 0·003 in. of diameter. To achieve this it is usually necessary to warm up the shrink rings before assembly, and such warming up should be within the tempering range used. The die proper is fully heat-treated and tempered and ground



(*left*)
1½-in. sq. × 1½-in. thick
(*right*)
4 in. × $\frac{3}{16}$-in. thick

28 *Cold upsetting of mild-steel billets. Load about 1500 tons*



(*a*) 2 in. × 0·6 in. (*b*) 4·1 in. × $\frac{3}{16}$ in.

29 *Cold upsetting of mild-steel billets, load about 1500 tons*

externally before assembly. The bore is finally ground to size after the assembly of all shrink rings.

The assembly order is as follows:

(i) shrink the inner ring on to the die
(ii) grind the outside diameter accurately to size after shrinking
(iii) shrink the outer ring on to the assembled inner ring and die.

The shrink-ring material usually used is similar to En 25:

0·27–0·35%C, 2·3–2·8%Ni, 0·5–0·8%Cr, 0·4–0·7%Mo.

After finish-turning, all shrink rings are heat-treated to about 80 tons/in² and tempered at about 450°C. They are then heated to 350°C to expand sufficiently for engagement with the mating rings and allowed to cool in position. Parallel bores are preferred to tapered bores, being simpler to manufacture and to measure.

COLD EXTRUSION OF GEARS

In view of the economic factors militating against the manufacture of simple tubular components by cold extrusion, attention has been paid to the manufacture of more difficult objects such as gear wheels. A process was developed on the basis of a consideration of the volume of material required to produce an extrusion using the smallest power, and success has been experienced in making gear blanks in three steels (Table I).

These components are illustrated in Fig.16*a* and the gears produced in Fig.16*b*.

All finished components were stress-relieved at 400°C for 1 h. after which the physical characteristics observed were:

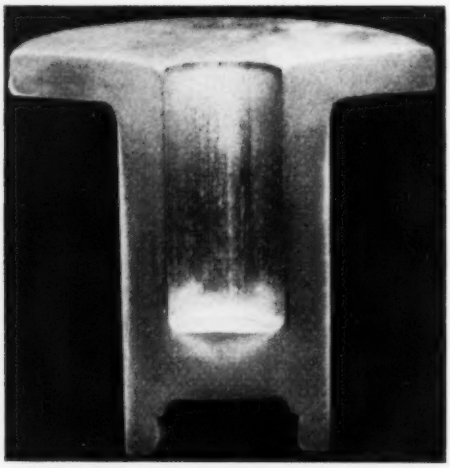
	UTS tons/in²	YS tons/in²	Elong. $4\sqrt{A}$,%	R in A,%	Izod, ft/lb
0·08%C	42·2	41·8	14	54	8
En 3*A*	52·2	50·7	11½	44	13·3
En 17	61·8	57·2	12	47	14·8

Stroke/load curves are shown in Fig.17. Section and tooth hardness figures are as shown in Figs.18 and 19.

Two types of gear blank were made and the internal

dia. across horns 2¾ in. thickness 0·34 in.

30 *Billet 1⅜ in. dia. × 0·72 in. in mild steel, formed to shape, increasing the face area from 1·49 in² to 3·14 in²*



i.d. 0·88 in. o.d. body 1·58 in. o.d. flange 3·15 in.
length 3·2 in. flange thickness 0·35 in.
cross-sectional area of body 0·61 in² reduction 39%
Load: form about 300 tons, pierce about 70 tons

31 *Flange forming and extrusion from a plain billet*

splines and gear teeth were formed at the same time. Dimensional characteristics are as follows:

Component no.1

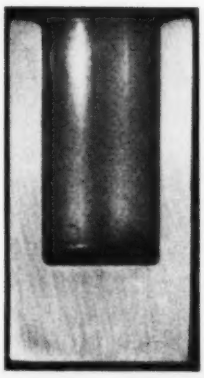
External	o.d. 2·655 + ·002 in.	Pressure angle 25°, 27 teeth
	Root dia. 2·338 in.	
Internal	Six splines, width 0·342 in., depth 105 in.	
	Bore dia. 1·19–0·0005 in.	

Component no.2

External	o.d. 3·845 + 0·005 in.	Pressure angle 20°, 28 teeth
Internal	i.d. 2·370 in.	Pressure angle 25°, 27 teeth
	Root dia. 2·662 in.	

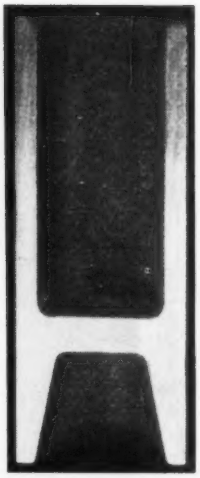
Recently various other steel qualities (Table II) have been put through the smaller of the two tool sets.

In general the gears in these steels were required for subsequent heat-treatment and the properties attained by cold working are of no consequence. The extrusion loads for En 18 and En 361 were in the same order as for the En 17 steel illustrated, whilst those for the remaining three were in the region of 375–400 tons.



i.d. 0·8 in. o.d. 1·225 in.
length 2·2 in.
cross-sectional area of billet 1·18 in²
reduction 43%
extrusion load 68 tons = 136 tons/in²

32 *Projectile component, backward extrusion*



i.d. 0·85 in. o.d. 1·18 in.
length 3 in.
cross-sectional area of billet 1·09 in²
reduction 55%
extrusion load about 77 tons = 136 tons/in²

33 *Projectile component, backward extrusion*

ANALYSIS OF THE DEGREE OF STRAIN AND HARDENING OF AN EXTRUDED CYLINDER

A component was made by single impact from a cylindrical billet to the proportions and hardness figures shown in Fig.20.

It is interesting to note the pattern of hardness figures that follows the strain pattern on the material; this is illustrated clearly by analogy with the four stages of extrusion of a laminated Plasticene billet (Figs.21*a–d*).

It is also interesting to note the degree of elongation each particular part of the surface receives during extrusion. To determine this, a grid was engraved on the cut face of a longitudinally sectioned billet which was then extruded. It was not practicable to photograph the deformed grid but Fig.22 illustrates the results obtained.

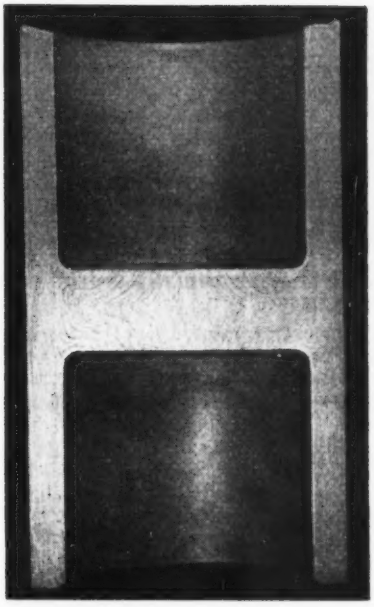
On the left of Fig.22 is a diagrammatic representation of the billet. The vertical markings *A–J* are shown in full and the resultant pattern after extrusion is on the right. For clarity, the horizontal markings shown (S–Z) on the billet (left) are the original boundaries of the various degrees of surface elongation found after extrusion (right).

For this extrusion the material used has been 0·08%C steel and commercial mild steel (STA/5V3), typical percentage analysis being:

	C	Mn	Si	S	P
Low-carbon steel	0·08	0·12	0·07	0·020	0·016
Mild steel	0·21	0·42	0·20	0·041	0·036

i.d. 1·5 in.
o.d. 1·9 in.
length 2·2 in.
cross-sectional area 1·07 in²
reduction 62%
extrusion load about 260 tons = 150 tons/in²

34 *Steel energizer container, backward extrusion*

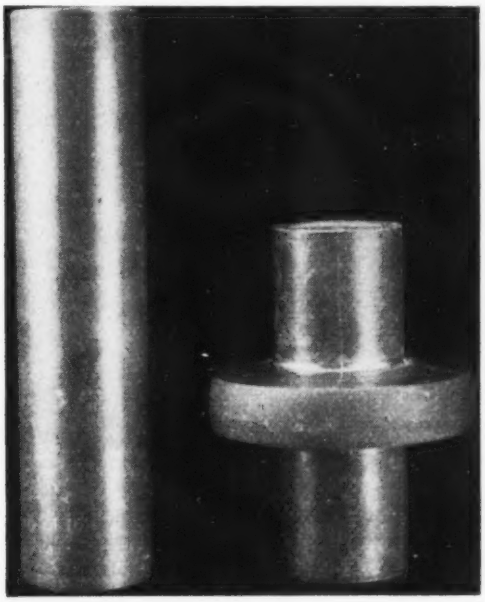


i.d. 2·4 in. o.d. 3·08 in. length 6 in.
thickness through mid-section 0·55 in.
cross-sectional area 4·5 in² reduction 60%
load about 660 tons

35 *Combined backward and forward extrusion from round billet*

The strength characteristics of these steels are shown by the mechanical test results before and after extrusion (Table III).

The tests in the extruded condition were from miniature test-pieces cut from the walls of the tubes,



36 *Swelling mid-section of 1-in. dia. bar in mild steel, billet originally 4¼ in. long. In the finished pressing the mid-section increased from 1 in. to 2⅛ in., an increase of 350%*

billet dia. 3·1 in. thickness 2 in.
cross-sectional area of billet 7·55 in²

37 *Squeezing a square-section bar into a round billet*

the hardness of which shows considerable variation as has already been shown. This factor is probably responsible for the variation in elongation and particularly in impact values, where it has been shown that the result can vary with the position of the notch in relation to the more heavily worked inner surface of the tube.

PUNCH LOADING OBTAINED IN PRACTICE

Some important figures on punch loading are recorded in the following notes relating to the single-shot extrusion of a shell body shown in Fig.23. This work was carried out on two steels:

i.d. 2·1 in. o.d. 3·1 in. length 3·3 in.
cross-sectional area of billet 7·55 in² reduction 46%
extrusion load about 450 tons = 130 tons/in²

38 *Extrusion of formed billet*

	Rimming steel	Killed steel
C, %	0·09 (rim) 0·03 (core)	0·08
Mn, %	0·16	0·12
Si, %	0·02	0·07
S, %	0·055	0·02
P, %	0·020	0·016
Ni, %	0·09	0·20
Cr, %	0·04	0·15
Cu, %	0·05	0·33

All billets were subcritically annealed to a hardness of about 120 DPN and after dimpling were re-annealed and single-shot extruded.

Various tool steels were used, all of which failed by swelling, bending, or fracture, and no steel was successful for continuous operation.

Punch loadings were as follows:

	Total load, tons	tons/in² Minor punch dia.	Major punch dia.
Initial extrusion on small dia. of punch	160–170	152–161	120–128
Extrusion on to major dia.	200–210	188–197	151–158

The strength requirement is far higher than could normally be expected from a tool steel and 120 tons/in² is considered to be the absolute maximum design load for tools of this nature with a preference, for production purposes, of 100 tons/in² maximum. It was observed that the tools which bent or became swollen did so at about 180–200 tons. Above 200 tons (press loading) the tools fractured near the nose.

PUNCH-NOSE PROFILE AND THE EFFECT UPON EXTRUSION LOAD

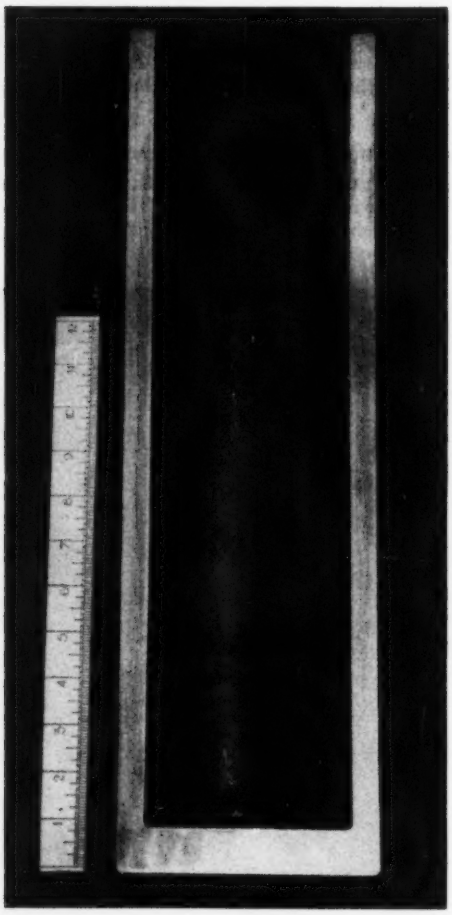
Various trials were carried out to assess the best punch-nose profile. These trials consisted of:

(i) flat-nosed punches with varying radii at the junction between the flat nose and the side

(ii) punches with coned nose form.

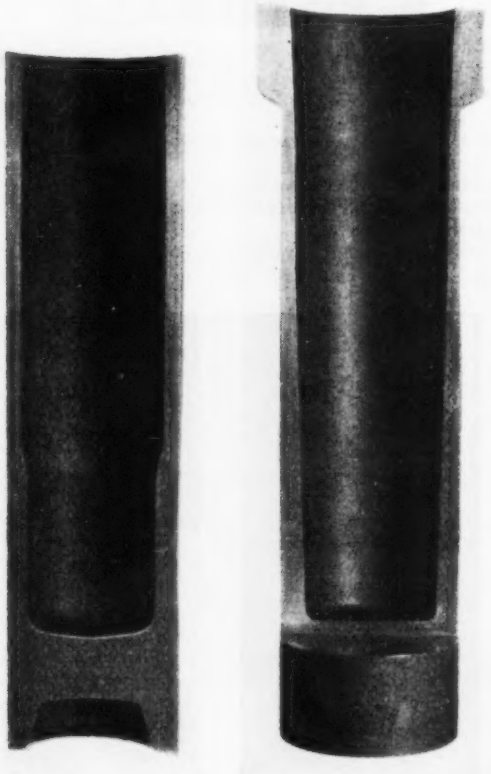
The results of these trials are illustrated in Fig.24 from which the following conclusions are clear.

A conical shape to the nose of the punch reduces the punch loading required until a breakdown in the



i.d. 4·45 in. o.d. 5·7 in. length 18½ in.
cross-sectional area of billet 12·6 in² reduction 69%
load 2000 tons

39 *Hydraulic buffer, backward extrusion*

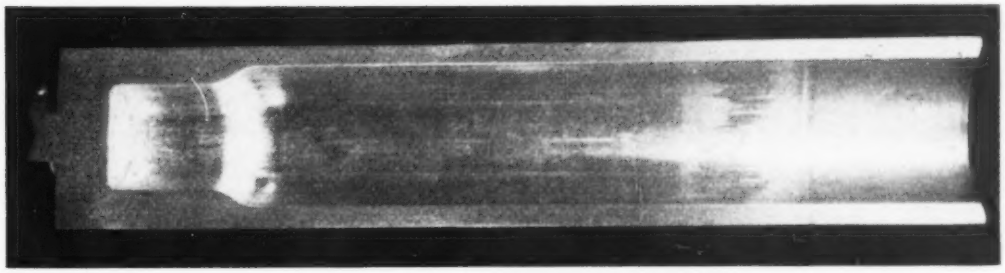


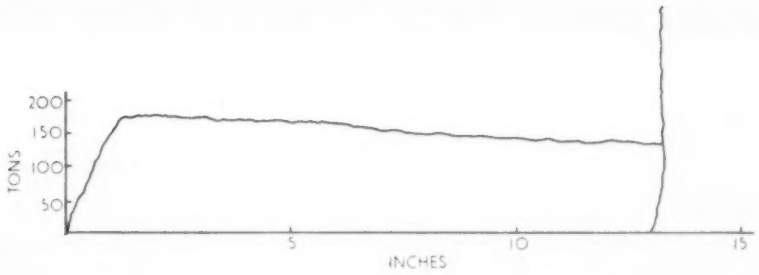
(a) (b)

(a) i.d. 1·24 in. o.d. 1·565 in. length 6 in.
cross-sectional area 0·7 in² reduction 68%
extrusion load 200 tons

(b) mean i.d. 1·285 in. o.d. 1·56 in. length 6¼ in.
mean cross-sectional area 0·615 in² reduction 33%
extrusion load 75 tons

40 *(a) 40 L.70 shell, backward extrusion, (b) forward extrusion, multi-operation*





i.d. 1·734 in. o.d. 2·312 in.
length 12½ in.
cross-sectional area 1·88 in²
reduction 55%
extrusion load 180 tons

41 *Rocket component, 4½%Cu–Al alloy HE–15, backward extrusion*

phosphate and lubrication causes a steeply increasing load requirement.

Flat-nosed punches maintain the phosphate and lubrication at the base of the cavity and give a regular and increasing load requirement in accordance with the depth of penetration until nearly maximum penetration is achieved, after which the load requirement tends to fall.

The degree of radius at the junction between the flat nose and the side is important in reducing punch loading.

From the graphs it will be seen that maximum punch loading varied from 137 to 157 tons/in² and hence a reduction of nearly 13% can be achieved with the best radius.

WARM TEMPERATURE EFFECTS UPON THE EXTRUSION OF STEEL

Certain work has been done in an endeavour to reduce the load on the extrusion punch. Trials were carried out in the laboratory using a testing machine which extruded at the rate of 6 in./min punch speed. The results are given in Table IV.

Higher temperatures than 200°C were not successful, since the lubrication tended to char.

It was clear from the analysis of the hardness figures of the resulting components that the extrusion in each case was following a somewhat different pattern. The mean hardness figures of mid-wall positions were as follows:

Billets before extrusion	111 DPN
Component extruded at ambient temperature	208 ,,
Component extruded at 100°C	214 ,,
Component extruded at 200°C	220 ,,

The components are illustrated in Fig.25. Unfortunately *D* 1 was destroyed on being extracted because of damage caused by the broken bolster but the base thickness was in the same order as that of *B*1.

However, in specimens *B*, *C*, and *D* the base thickness is not significant, since deformation is proceeding at below the peak load and the final base thickness depends very much on the operator in the absence of positive stops in the rig.

Sample *C*1 shows a base thickness of about half the wall thickness and a microscopical examination fails to reveal anything other than a suggestion of incipient shear on the bottom radius. This is contrary to normal results, where a shear crack forms on the base/wall junction at a base thickness of 1*T* and thereafter there is a very sharply rising load requirement to bring about further base reduction. It will be very interesting to continue these trials to ascertain the role of temperature in this respect.

Following the laboratory trial, a further trial was carried out on factory plant using an existing set-up currently in production. In this, the component was a combined forward and backward extrusion (Fig.26). Curves were taken using an automatic indicator which yielded the result shown in Fig.27. The first extrusion peak is caused by the forward extrusion of the billet into a reduced die annulus. The second extrusion peak is caused by the subsequent backward extrusion. A substantial difference in punch loading is indicated by the curves and also by the figures given in Table V.

These trials seem to establish a greatest reduction in punch loading at 150°C.

At this stage it is not possible to say whether this reduction is due to more efficient lubrication performance or to physical characteristics in the steel, but experiments with and without certain lubricant additives seems to indicate that it is a combination of both. The physical characteristic change would be the strain-ageing phenomena, on which very little data appear to be available at these temperatures associated with the speed of working. This development is as yet in its infancy but the facts obtained so far have been recorded in this paper for information.

Extrusion and upsetting of various components

Figures 28–41 show a variety of extruded components in mild steel and alloys.

Hexagonal carbide and nitride phases and their occurrence in some alloy steels

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SYNOPSIS

Hexagonal carbides and nitrides which involve a close-packed arrangement of transition metal ions are described and lattice parameters are tabulated for all the known examples of such compounds. These phases are characterized by the formula M_2X where M represents one of the transition metals, V, Cr, Mn, Fe, Co, Ni, Nb, Mo, Ta, *or* W, *and X represents* C *or* N. *The interstitial sites taken up by* C *or* N *need not all be occupied and solid-solution ranges may also include the formula M_3X or other simple proportions that could be significant. Several solid-solution phases of this structure have been obtained in residues separated from alloy steels by electrolysis, and significant relationships exist between the crystallographic parameters of such phases and the composition of the steel. These relationships are a guide to the composition of the phases, and together with other observations about their occurrence give a general picture of their behaviour and importance in connection with the metallurgy of alloy steels.*

Various aspects of the constitution and nature of these phases are considered including their relationship to the analogous cubic MX-type carbides and nitrides. 1648

INTRODUCTION

THE STUDY of the mode of occurrence, the chemical constitution, and structure of the carbides and nitrides in alloy steels is of considerable interest in view of the important influence which these compounds may have on the properties of the steels. This interest has been recently reflected in a conference at Sheffield University on precipitation processes in steels,[1] and in a notable number of papers dealing with one aspect or another of this subject.

Continuing their own work on the X-ray examination of carbides and nitrides after electrolytic isolation from steels[2] the authors have encountered a number of hexagonal carbide and nitride phases. These appear to be isomorphous with Mo_2C, Fe_2N and several other carbides or nitrides of pure transition metals, but considerable variations in composition are possible. The stabilization of such hexagonal phases at certain ratios of $M : X$ might be explained in similar terms to the interpretation of cubic carbides and nitrides put forward by Rundle[3] and Hume-Rothery.[4] A notable advance was made in the general fundamental understanding of carbides which may occur in alloy steels when Goldschmidt's review appeared in 1948.[5] At that time, however, only a few carbides of the type referred to were known. Several more carbides and nitrides have since been reported.

The paper summarizes the available data on carbides and nitrides in this class. Information is then given relating to 23 isomorphous phases which have been found in alloy steels. These phases could contain either carbon or nitrogen, or both elements, and varying proportions of metallic elements. It is a difficult matter to elucidate completely the composition of such phases since they have invariably occurred in association with other constituents.

In certain steels, phases of this type have been found to play an important part in the early stages of a precipitation or carbide reaction sequence and may be valuable in producing precipitation hardening. This function has been more clearly established by the use of electron microscopy and diffraction.[1]

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TABLE I Hexagonal carbides and nitrides of 'M_2X' type

System	Formula, etc. (at—% where stated)		a, kX	c, kX	c/a	Ref. no.	Comments
Ni–C	Ni_3C		2·626	4·305	1·639	(9)	Exceptionally high value of c/a
Co–N	γ Co_3N 'N poor'		2·658	4·351	1·637	(9)	
	'N rich'		2·866	4·358	1·635		
Ni–N	Ni_3N	(*a*)	2·6623	4·3035	1·6165	(9) (11)	
		(*b*)	2·6646	4·298	1·613		
Fe–C	ϵ-carbides	Fe_2C	2·749	4·340	1·579	(9) (11)	
		Fe_3C	2·724	4·321	1·580		
Fe–N	Fe_3N	23·9 % N	2·695	4·362	1·619		Shown in Fig. 1
		26·9 % N	2·716	4·378	1·612		
	to	28·7 % N	2·729	4·388	1·608	(13)	
		33·0 % N	2·759	4·411	1·599		
	Fe_2N	33·3 % N	2·761	4·413	1·598		
	Fe_3N	27·2 % N	2·7175	4·385	1·614		
	(approx.)	29·2 % N	2·7365	4·400	1·609		
		29·7 % N	2·7425	4·400	1·604	(12)	
	to	31·0 % N	2·7465	4·404	1·604	(9)	Not included in Fig.1
	Fe_2N	33·0 % N	2·7584	4·411	1·599		
Cr–N	Cr_2N	N–poor	2·747	4·439	1·616		
	(approx.)	N–rich	2·770	4·474	1·615	(9) (Work of Blix)	Two representative points in Fig.1
		27·6 % N	2·742	4·429	1·615	(9)	
		34·8 % N	2·769	4·470	1·614	(Eriksson)	
	'Cr_2N phase'		2·740	4·451	1·624	(10) (Vol. 13 p. 82)	
Mn–N	Mn_2N phase (?)		2·778	4·500	1·62	(10) (Vol. 15 p. 99) (80)	
		28·4 % N	2·773	4·520	1·630	(9)	
		30·5 % N	2·800	4·524	1·616		
	Phase including	31·9 % N	2·812	4·526	1·610	Work of G. Hägg	Some points included in Fig.1
	Mn_2N	32·5 % N	2·816	4·526	1·607	(1929)	
		33·4 % N	2·821	4·527	1·605		
		35 % N	2·828	4·528	1·601		
V–N	$V_2N_{1·08}$	26·47 % N	2·831	4·533	1·601	(9)	Actually between V_3N and V_2N
	$V_2N_{0·9}$	31·03 % N	2·835	4·541	1·602		
V–C	(V_8C_3)	27·0 % C	2·875	4·538	1·578	(9)	i.e. V_2C but can have lower C (approaching V_3C)
	V_2C	33·3 % C	2·900	4·588	1·582		
W–C	W_2C		2·987	4·714	1·578	(9)	Mean of two results (close together)
Mo–C	Mo_2C	(*a*)	2·994	4·722	1·58	(9)	
		(*b*)	3·006	4·725	1·572		
	$Mo_2C \rightarrow$	25 % C	2·960	4·620	1·560	(9) (14)	Limits of phase region which apparently extends to Mo_2C
	Mo_2C	31·15 % C	3·001	4·723	1·573		
Ta–N	($Ta_2N_3 \rightarrow$)	29·1 % N	3·042	4·905	1·612	(9)	
	Ta_2N	33·3 % N	3·0415	4·9088	1·614		
Nb–N	Nb_2N	32·6 % N	3·057	4·947	1·624	(9)	
	($Nb_2N_3 \rightarrow$)	28·6 % N	3·046	4·946	1·624	(9) (15)	
	Nb_2N	33·3 % N	3·045	4·987	1·637		
Ta–C	Ta_2C	(Phase limits)	3·088	4·908	1·590	(9)	
			3·105	4·938	1·590		Results from two original authors
	(Ta_8C_3)	27·5 % C	3·095	4·927	1·592		
	Ta_2C	33·3 % N	3·098	4·931	1·592	(9)	
Nb–C	($Nb_3C \rightarrow$)	25·9 % C	3·1109	4·944	1·589		
		31·0 % C	3·1127	4·946	1·589	(9)	
	to	32·9 % C	3·1131	4·949	1·590	(16)	
	Nb_2C	33·3 % C	3·1198	4·958	1·589		

TABLE II Other hexagonal phases involving solid solution and replacement of C, or N, or metallic elements, or both

Systems involved	Formula and composition, at -%	*a*, kX	*c*, kX	*c*/*a*	Reference and notes
Fe–C–N	$Fe_2(N,C)$ 26·9% C + N, C/N = 0·7	2·706	4·364	1·613	(9), (13) Three points shown in Fig.1
Carbo-nitrides	28·7% C + N, C/N = 1·21	2·720	4·366	1·605	
	$Fe_2(N,C)$ C/N = 0·28	2·758	4·402	1·596	
	C/N = 0·17	2·751	4·408	1·602	
Co–C–N	$Co_2C_{0·18}N_{0·87}$	No lattice parameters available			(9) (Work of K. H. Jack)
Mn–C–N Ni–C–N	ϵ-Ni_2 (C,N)	"	"	"	(9), (13) Reported to form carbo-nitrides
Ta–C–N	Ta_2 (C, N)	"	"	"	(9) Complete solid solubility reported
V–C–O	V_2 (C,O) $VC_{0·42}O_0 \rightarrow_{0·1}$	2·875	4·545	1·581	(9) Spacings within range for V_2C phase in Table I. (Not shown in Fig.1)
	$VC_{0·5}O_0 \rightarrow_{0·1}$	2·895	4·570	1·579	
Fe–Cr–N	$(Fe, Cr)_2N$	No lattice parameters available			(9) Stated to have been observed in nitrided Fe–Cr alloys
Fe–Ni–N	ϵ-$(Fe,Ni)_2N$	"	"	"	(9) Found in nitrided Fe–Ni alloys
Cr–Mo–N	Cr Mo N_x	2·83	4·56	1·61	(9), (17) From 16–25–6 (Cr–Ni–Mo) steel
Cr–Mo–N–C	$(Cr,Mo)_2(C,N)$ C : N = 11 : 1 Cr : Mo = 16 : 9	2·884	4·551	1·578	(18) From a complex Ni-base alloy (Complete chemical analysis given)

BINARY CARBIDES AND NITRIDES

The available data on the lattice parameters of hexagonal nitrides and carbides are listed in Table I in general order of increasing parameters of pure binary systems, variable $M : X$ ratios are also included. This table refers only to nitrides or carbides of single metals, but not to solid solutions apart from composition variations in the pure binary systems involved. The information has been collected from certain recent tabulations; in particular a handbook of data on metals and alloys by Pearson[9] and volumes in the 'Structure Reports' series[10] with occasional reference to the original sources.

Table I includes only carbides and nitrides in which the metal atoms have a close-packed hexagonal arrangement. In this connection it should be mentioned that because of ordering of the interstitial atoms in certain cases the true unit cell may have a different a parameter from that given which refers to the hexagonal lattice of metal atoms alone. The data thus provide a single basis for comparison. Where desired the conversion $a^1 = a\sqrt{3}$ has been used. One consequence is that axial ratios all lie in the region of 1·58 to 1·62 which indicates how near the arrangement of metal atoms corresponds to the closest packing of spheres ($c/a = 1·633$). Another feature of Table I is that all parameters are given in kX units, which are the practical units employed in the authors' laboratory and elsewhere.

In Fig. 1, the lattice parameters of these phases are plotted, c as ordinate and a as abscissa, so that points of equal axial ratio would lie on straight lines. Four such lines of constant c/a are shown. In this diagram nitrides are represented by squares and carbides by circles. Some of the lattice parameters for intermediate compositions within the binary solid-solution range (crosses) and the phases found in steels (plus signs) have also been included.

TERNARY OR MORE COMPLEX CARBIDES OR NITRIDES

In addition to the binary solid-solution effects already covered in Table I, there have been a number of cases where more complex solid-solution replacements have been reported. For example, in addition to notable work on the binary systems Fe–C, Fe–N, Jack[11–13] described solid solutions in the system Fe–C–N with replacement of C by N in the ϵ-carbides. The same author also reported similar phases in the systems Co–C–N, Mn–C–N, and Ni–C–N. These and other examples from the literature, including metal atom replacements, have been summarized in Table II. Lattice parameters have not always been given, but where available these are included.

Some of the parameter values in Table II are also represented in Fig.1, by crosses. The phase reported by Beattie and Ver Snyder[17] had been found by X-ray diffraction examination of residues separated from a steel by electrolysis. The somewhat similar phase reported by Hobson[18] from a nickel-base alloy also calls for comment. The author described this phase as a solid solution between Cr_2N and Mo_2C. In terms of lattice parameters it lies between these two phases almost exactly where it would be expected given the porportions of Cr and Mo indicated by chemical analysis. Since the C : N atom ratio is high (11 : 1) it is predominantly carbide. Hence, although a carbide in this class of formula Cr_2C is not known, this solid-solution phase goes a considerable way towards this hypothetical end member.

CARBIDES AND NITRIDES FOUND IN STEELS

Experimental work

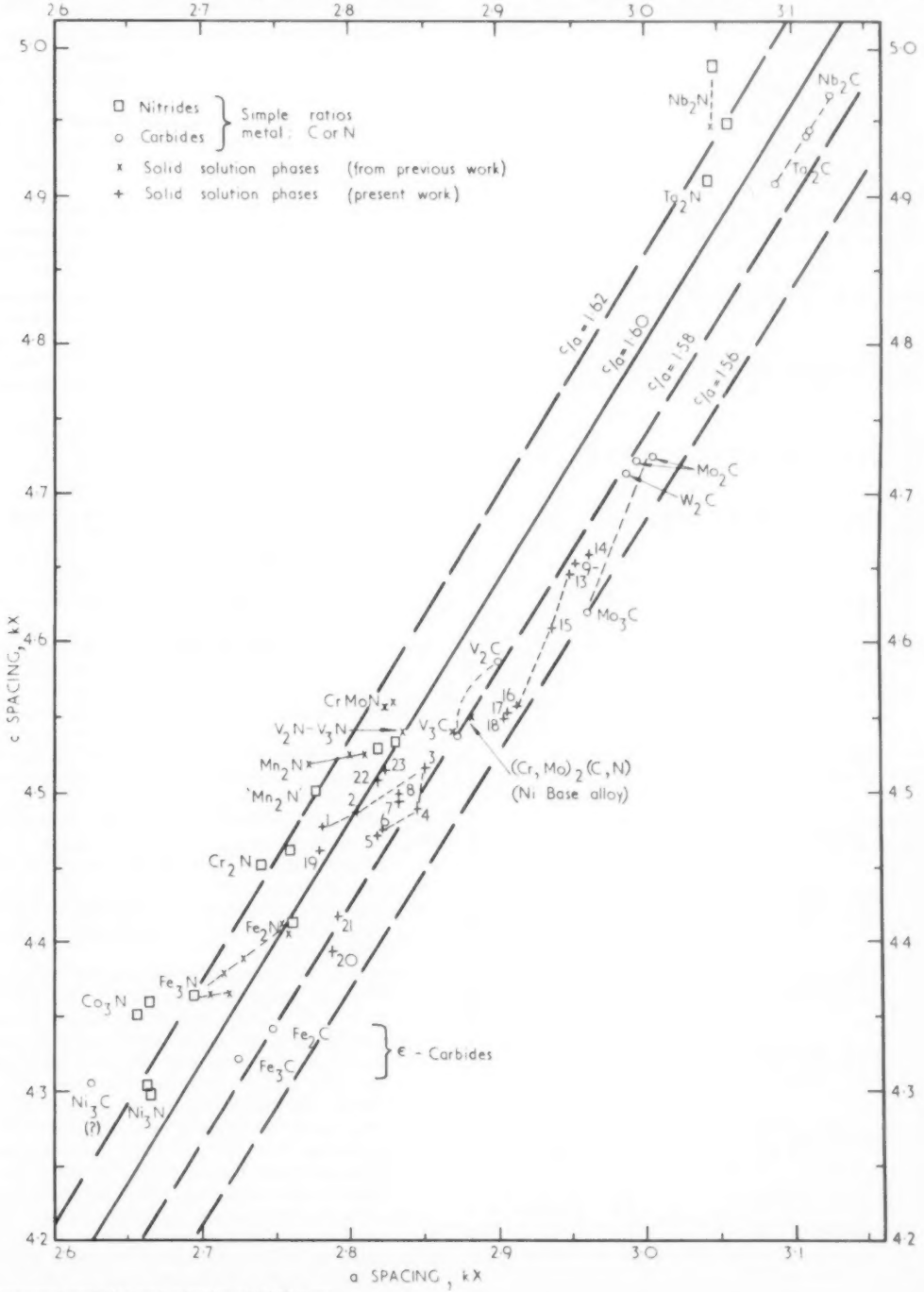
The steels in which hexagonal carbide and nitride phases have been found have generally been experimental steels produced in the laboratory in connection with a number of researches concerning the structure and properties of alloy steels. In the present context the authors are interested in the relationships between the chemical compositions of the steels and the crystallography of the hexagonal carbide and nitride phases.

The methods used in the separation and investigation of such microconstituents have been described by the authors elsewhere.[2] In the present work the phases referred to were invariably associated with other constituents. The authors intend to examine the possible uses of X-ray fluorescent analysis of residues, but the problem of the partition of alloy elements will still remain unless techniques for phase separation can be devised. It is for this reason they consider, that correlations between the crystallographic parameters of the microconstituents and the composition of the steels give the most satisfactory, although incomplete,

account of individual constituents available at present.[6–8]

Data obtained for 23 phases

The data obtained by these methods for 23 carbide and nitride phases are listed in Table III. Only examples are given for which satisfactory lattice parameter measurements could be obtained. In a number of other steels, traces or small amounts of the same type of hexagonal phase were encountered and in a few instances would have served to amplify the results in Table III. These examples are, however, more than sufficient to illustrate a number of points about the mode of occurrence of the phases. Each entry in



1 *Hexagonal carbides and nitrides of* M_2X *type*

TABLE III Hexagonal carbide–nitride phases separated from steels

Serial number	Chemical analysis of steel from which extracted, wt-% C	Mn	Si	Ni	Cr	Mo	V	N	N/C	Heat-treatment Time, h	Temp., °C	Lattice parameters, kX a	c	c/a	Notes (composition changes, etc.)
1	0·154	1·08	0·24	2·11	10·05	2·58	...	0·099	0·64	50	650	2·783	4·476	1·608	Increase of V 0–¼%
2	0·148	1·08	0·52	2·13	10·20	2·52	0·21	0·095	0·64	50	650	2·806	4·486	1·599	Further increase ¼–½% Also lower N : C ratio
3	0·116	6·28	0·29	8·32	14·13	2·65	0·53	0·044	0·38	1000	750	2·851	4·516	1·584	½–1% and higher Mn, Ni, Cr
4	0·114	6·24	0·25	8·19	14·14	2·08	0·52	0·050	0·44	1000	750	2·845	4·487	1·577	Decreasing Mo in steel
5	0·125	5·90	0·32	8·36	14·30	1·53	0·50	0·059	0·48	1000	750	2·819	4·473	1·587	(Nominal 2½–2–1½)
6	0·114	6·00	0·235	10·00	14·15	1·56	0·51	0·055	0·48	1000	750	2·822	4·475	1·586	+ 2% more Ni
7	0·116	6·18	0·30	10·00	14·60	2·15	0·54	0·045	0·39	1000	750	2·834	4·495	1·586	Increasing Mo content (1½–2–2½) nominal
8	0·105	5·82	0·285	9·92	14·26	2·65	0·54	0·050	0·48	1000	750	2·834	4·498	1·587	
9	0·104	0·51	0·30	...	2·15	1·03	...	0·022	0·21	20	600	2·951	4·648	1·575	
10	,,	,,	,,	,,	,,	,,	,,	,,	,,	100	600	2·949	4·646	1·575	Increasing time at 600°C
11	,,	,,	,,	,,	,,	,,	,,	,,	,,	400	600	2·950	4·650	1·576	
12	,,	,,	,,	,,	,,	,,	,,	,,	,,	20	650	2·953	4·652	1·575	Time at 650°C Increasing temperature
13	,,	,,	,,	,,	,,	,,	,,	,,	,,	100	650	2·953	4·650	1·575	
14	,,	,,	,,	,,	,,	,,	,,	,,	,,	20	700	2·963	4·658	1·572	2% Cr, 1% Mo, ½% V more
15	0·105	0·68	0·08	...	4·08	2·07	0·44	0·021	0·20	1000	550	2·937	4·608	1·569	
16	0·090	0·52	0·30	...	6·02	1·95	0·52	0·021	0·23	1000	550	2·907	4·552	1·566	
17	,,	,,	,,	,,	,,	,,	,,	,,	,,	20	600	2·914	4·556	1·564	Same steel two temperatures and times 2% more Cr
18	0·108	0·60	0·39	...	5·95	2·01	0·98	0·021	0·19	400	550	2·905	4·550	1·566	
19	0·103	0·63	0·30	...	5·20	1·08	...	0·027	0·26	400	700	2·78	4·46	1·604	½% more V
20	0·140	0·56	0·21	2·15	11·84	...	...	...	...	1	650	2·788	4·393	1·581	As nos.9–14, but + 3% more Cr
21	0·117	0·53	0·06	2·07	11·92	2·04	...	...	...	1	650	2·792	4·417	1·582	Increasing Mo content
22	0·113	0·54	0·17	2·16	11·84	3·10	...	...	...	1	650	2·820	4·508	1·599	
23	0·111	0·51	0·18	2·04	11·92	4·07	...	...	...	1	650	2·825	4·514	1·598	

Table III is as far as possible also represented in Fig.1 by a plus sign.

The steels are grouped in Table III according to their origin, composition variations, or heat-treatment. The changes covered within a given group of steels are indicated in the right-hand column of the table. Significant relationships are represented by Figs.2–6 and these illustrations bring out the following points:

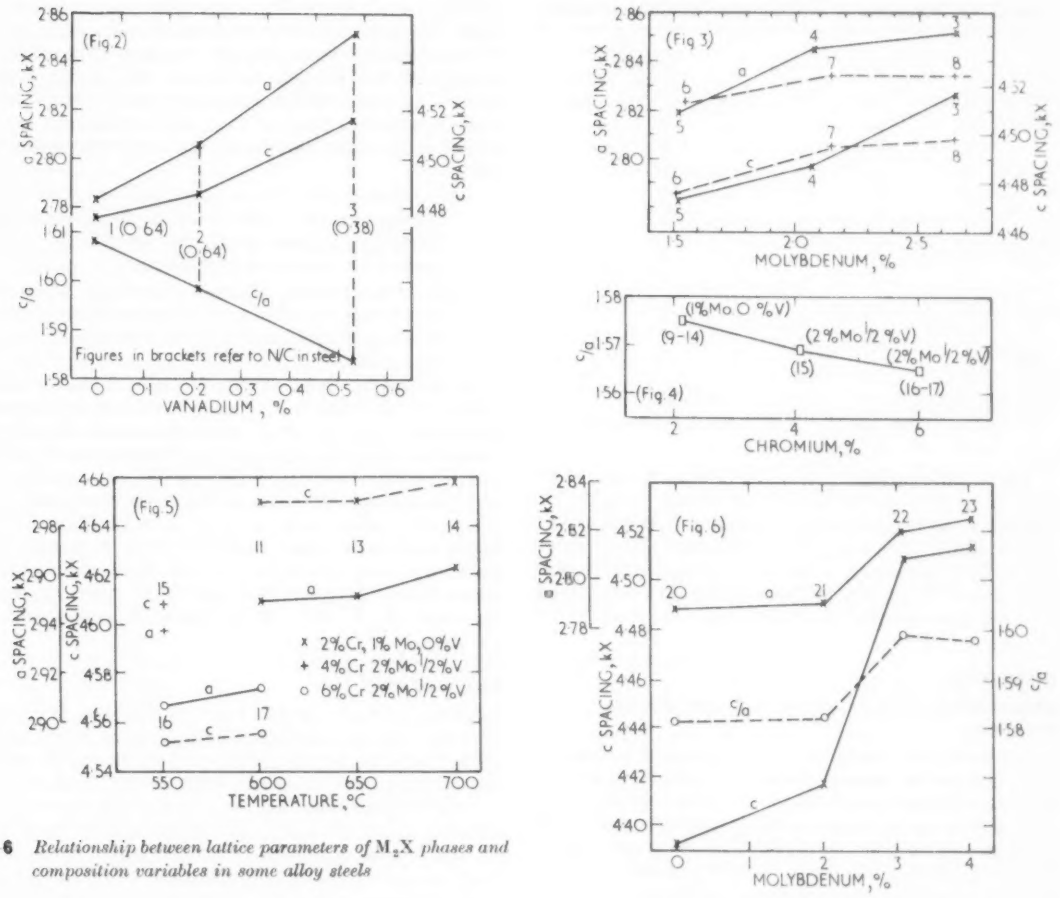
(i) the parameters a and c, and c/a in Fig.2, show progressive changes with vanadium content in the steel. There is, however, quite a high ratio of N : C in the first two steels and a lower ratio in the third. This may be partly responsible for the pronounced drop in axial ratio (*see* discussion). Changes in Mn, Ni, and Cr in the steel do not appear to be relevant

(ii) in Fig.3 the effect of increasing molybdenum in the steel is to increase the lattice parameters a and c but not c/a. The series 5→4→3 correspond to roughly 8%Ni, and the series 6→7→8 to 10%Ni. It is possible that the lower parameters for steel 8, in contrast to no.3, result from some Ni entering into the M_2X phase. According to Fig.1 all the phases in steels 1–8 are in the region near Cr_2N, but can obviously contain Mo and V and probably also some Fe and Ni

(iii) in Fig.4 the steels from which the phases 9–19 were obtained belong to a series. One feature which may be significant is a decrease in axial ratio with Cr content as shown. There is reason to believe that this is significant since Mo does not appear to have this effect. V probably does give a decrease and could partly be responsible for some of the drop from 14 to 15, but not from 15 to 16 or 17 where there is no further change in V content

(iv) further details in relation to these steels are brought out in Fig.5. There is the general dependence of both a and c parameters on composition (*see also* Fig.1) and a slight, but definite increase with temperature. In low-Cr steels the phase appears to be nearer to Mo_2C, but at higher Cr contents and also with V additions the phase approaches nearer to the V_2C type. Its general formula becomes $(CrMoV)_2(CN)$. At higher temperatures the phase contains more Mo in solid solution, but this effect is much less marked than the effect of an overall increase in Cr content. No significant effects of time at temperature are suggested by the available data so far

(v) the four steels (nos.20–23) represented in Fig.6 have the same base composition with about 0, 2, 3, and 4 Mo respectively. There is again a pronounced increase in both a and c parameters due to the presence of Mo in the M_2X phase. In this case the phases appeared after only 1 h at 650°C. The same phases also appear in Fig.1. The steel containing no molybdenum has apparently formed a carbide which has proportions of metal atoms nearer to the base composition than was observed in the other series of steels, and can here be regarded as an ε-carbide with some replacement of Fe by other atoms notably Cr. The addition of Mo to the steel gives a slight rise in parameters at 2%Mo, but the effect of the higher Mo contents is more marked.



2–6 *Relationship between lattice parameters of* M_2X *phases and composition variables in some alloy steels*

Occurrence of phases in relation to other constituents

Constituents of this type appear to have three possible roles according to the steel in which they occur:

(i) they may form as well defined crystalline phases from the matrix during a tempering or annealing treatment or during service at high temperatures. As such they could be stable end products, but might alternatively represent intermediate stages in a carbide sequence. Some of the results in Table III correspond to cases where the hexagonal phase is stable at least for appreciable times at temperatures as high as 750°C. In steels nos.9–19, however, the phase generally decreased in amount with time and vanished more rapidly at higher temperatures

(ii) in certain steels a phase of this type appears as a first stage in a precipitation process or carbide reaction sequence. Its existence may be transitory, but important in that it can lead to appreciable precipitation hardening. In these circumstances the phase has been more definitely identified by electron microscopy and diffraction. It may, as we have found, exist in a plate-like form so that some diffraction lines or rings are weak or missing. A colleague of the authors has mentioned an example elsewhere.[1]

Although this role might be considered as a limiting case of (i), the phase takes on an added significance under these conditions. Thus precipitation hardening obtained may depend initially on coherency with the lattice, whilst the resemblance here to the function of ε-carbide in the tempering of plain carbon steels at low temperatures is obvious. It could form coherently with very little or no diffusion of alloy elements. Subsequent diffusion could then lead to its replacement by other carbides

(iii) the phase may be almost or even completely inactive metallurgically. Thus Cr_2N has sometimes been found in chromium steels and its presence there suggests it has formed as an inclusion rather than as a metallurgical precipitant (*cf.* TiN). It might even however be possible to dissolve such a phase in the matrix at very high solution temperatures in which case a reprecipitation in category (i) or (ii) could follow. Small amounts of such phases (essentially Cr_2N) were also found in certain of the steels in Table III, but their presence bore no relation to the heat-treatment or to other constituents.

TABLE IV Existence of metallic and interstitial structures

Group	Element	Metal structures bcc	fcc	hcp	Carbides or nitrides Cubic MX	Hex M_2X
IV	Ti	E	...	E	E	...
	Zr	E	...	E	E	...
	V	E	...	...	E	E
V	Nb	E	...	...	E	E
	Ta	E	...	...	E	E
	Cr	E	(E?)	...	E	E
VI	Mo	E	...	...	E*	E
	W	E	...	...	E*	E
VII	Mn	E	E	...	...	E
	Fe	E	E	...	...	E
VIII	Co	...	E	E	...	E
	Ni	...	E	...	...	E

E signifies the phase is known to exist
* Cubic phases Mo_2N, and W_2N (less certain) have been reported.[9]

These observations give only a brief indication of the role of M_2X type of carbide or nitride phases. The authors hope to give further attention to the problems raised. Much of the experience and evidence reported so far have, however, depended on the unanticipated appearance of phases in the course of investigations into the constitution of alloy steels.

DISCUSSION

General nature of the hexagonal M_2X type carbides and nitrides

The phases are based on a close-packed hexagonal array of metal atoms. There are simple crystallographic relationships between the three simple structures taken up by many metallic elements. Secondly, cubic phases such as the MX-type carbides or nitrides TiC, NbC, TiN, ZrN, etc., could be considered to be derived from face-centred cubic modifications of the metals concerned, with the carbon or nitrogen atoms added. As pointed out by Rundle,[3] however, such phases must not be regarded as simple solid solutions of the interstitial elements, but as interstitial compounds (which may nevertheless have variable composition). The binary M_2X hexagonal phases are analogous. These relationships are suggested by the scheme:

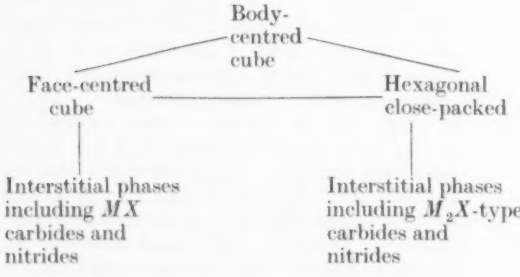

Body-centred cube
Face-centred cube
Hexagonal close-packed
Interstitial phases including MX carbides and nitrides
Interstitial phases including M_2X-type carbides and nitrides

Table IV summarizes the transition elements concerned here and shows which phases occur in the metallic state and in carbides or nitrides.

It was pointed out in the introduction that a fuller interpretation of these hexagonal phases might follow on the lines of that put forward for the cubic phases by Rundle[3] and Hume-Rothery.[4] The information in Table IV also amplifies and extends the observations of Goldschmidt[5] regarding the structure of carbides in relation to the periodic table, but it is unlikely that any really new principle emerges from Table IV. It is noted, however, that of the twelve elements listed, cobalt is the only certain exception to the following rule:

(i) elements which form a given close-packed modification in the metallic state form only that interstitial phase with the alternative close-packed arrangement of metal ions

(ii) both interstitial phases occur only when the metal does not itself possess a close-packed modification. (Cr is a possible but by no means certain exception.)

According to Table I the hexagonal phases often appear to be limited at or near to the M_2X composition which thus seems to be fundamental. The interstitial sites may not all be occupied and the composition can extend down to M_3X (25%C or N). Special significance thus attaches to ratios of 2 : 1 and 3 : 1. The other ratios such as 5 : 2 or 8 : 3 indicated in Table I are much more in doubt. It is, however, just possible that by analogy with some of the cubic phases which can lie between MX and M_4X_3, the hexagonal formulae M_2X and M_8X_3 have a corresponding meaning.

Axial ratio effect

A feature of Fig.1 should be mentioned here. It is clear that the known nitrides generally have axial ratios above 1·60 and mostly between 1·60 and 1·62, while the carbides have axial ratios between 1·56 and 1·60. The only exception is Ni_3C to which some slight doubt attaches. If this axial ratio effect applies to solid-solution phases, as it almost certainly does, it could be used to assist in the description of such phases separated from steels, i.e. whether a particular phase is predominantly a carbide or a nitride.

Solid-solution effects

The possible types of solid-solution effects are:

(i) incomplete filling of available interstitial sites giving variable compositions, usually between M_3X and M_2X (Table I), intrinsic to the general understanding of these phases. The interstitial solid solution is confirmed by the increasing lattice parameter with increasing N or C content

(ii) varying proportion of C or N in these sites (some of the solid solutions in Table II). The replacement of C or N by other interstitial atoms such as O or B is not excluded. The replacement of N by C giving the carbo-nitrides listed at the top of Table II clearly shows the axial ratio effect

(iii) varying proportions of metal atoms M. This type of solid solubility is represented by phases in Tables II and III

(iv) combinations of (ii) and (iii), or even of (i), (ii), and (iii). These are represented by the phase reported by Hobson (the last entry in Table II) which is nevertheless predominantly a carbide, and by some at least of the phases in Table III, although the proportion of N : C is apparently generally low.

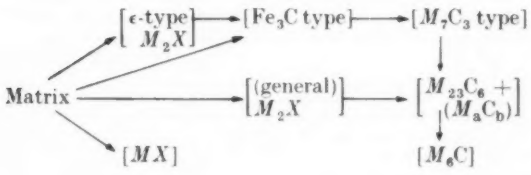
The existence of complete solid solutions between many pairs of carbides and nitrides of the cubic MX type is well established.[9] Incomplete solubility reflects larger differences in the atomic diameters of the metallic elements. Something similar should apply to the M_2X phases and complete solubility has been reported for the one instance investigated so far (Ta_2C–Ta_2N).

Phases found in steels

The phases found in steels are thus regarded as more or less complex solid-solution phases. The occurrence of two (or even more) of such phases is not excluded. Thus the occurrence of ϵ-carbide with relatively low parameters could be followed by phases such as $(Mo\ Cr)_2C$ in the same steel.

The data in Table III and Figs.2–6 have made clear the wide range of lattice parameters and solid-solution effects which are possible. The significant metallic alloy elements are V, Cr, Mo, and perhaps Ni. Most of the phases are predominantly carbides but where the axial ratio is higher there is probably a higher proportion of nitrogen.

Possible precipitation and reaction sequences for particular kinds of alloy steel have been described elsewhere.[1,2] A general scheme could involve three possible main strands:



It does not follow that any one phase is nucleated by the one before it; there can be independent nucleation, and there can be a considerable time period in which two phases are present together. Nor can every stage necessarily occur in every steel. Thus M_7C_3 is based on Cr_7C_3 (Mn_7C_3 is isomorphous with it) and generally only forms when Cr is present. The final step to M_6C usually requires the presence of molybdenum or tungsten although its formation in the presence of niobium (or tantalum) is possible. The authors have found one example of this. The appearance of a general precipitating phase of the M_2X type (often reported as 'Mo_2C') which may eventually be replaced by M_6C (with or without the interpolation of $M_{23}C_6$) appears to be separate from a general sequence which could be initiated by an ϵ-carbide, although this phase often appears to be very transient except at low temperatures and in low-alloy (or plain carbon) steels.

CONCLUSIONS

(1) Hexagonal M_2X types of carbide and nitride are analogous to the cubic 'MX' type.

(2) The formula M_2X appears to represent a stable combination and generally corresponds to the highest ratio of $X : M$. The interstitial sites may not, however, all be filled and the composition may extend down to M_3X. (Other ratios of $M : X$ might also be significant.)

(3) Extensive solid solutions can be expected between compounds of this type.

(4) Solid-solution phases have been found in a number of experimental alloy steels, and lattice parameters have been reported for 23 of these. The parameters show correlations with the amounts of alloy elements present in the steels thus giving evidence of their presence in the M_2X-phases. A complete interpretation is not possible for a number of reasons including the presence of other phases.

(5) Other aspects of the occurrence of such phases have been indicated, including a general scheme of carbide precipitation and reaction sequences in steels. In this scheme M_2X type phases may act rather like ϵ-carbide and give precipitation hardening or other effects at the earliest stages of a sequence. Alternatively a more general M_2X type of precipitation, presumably requiring some diffusion, may occur.

(6) These M_2X phases are, however, not as stable as the MX type of carbide and nitride, and are often eventually replaced by other phases, whereas the cubic type are not replaced.

(7) A general understanding of the place and function of the hexagonal M_2X type of carbides and nitrides is developing along these lines. The authors consider that they may well play a much more significant role than had previously been realized. Although analagous to the MX-type cubic carbides, their behaviour in steels is apparently somewhat different and they are, in general, less stable.

ACKNOWLEDGMENTS

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STAL *in English*

Number 5 (May 1959) of the cover-to-cover translation of the Russian journal *Stal'* has been published, and subsequent issues should appear at about monthly intervals. The contents of nos.6 and 7 are given below. Full details of subscriptions to *Stal in English* are available from the Secretary of The Iron and Steel Institute.

No.6

IRONMAKING

Primary slag formation using fluxed sinter
Hot-blast temperature drop in blow-pipes
Influence of hot-blast temperature on dimensions in the oxidizing zone
Reduction of iron-ore concentrates by carbon dioxide in a fluidized bed

STEELMAKING

Use of high-pressure natural gas for heating open-hearth furnaces
Ports in gas-fired open-hearth furnaces when feeding compressed air and oxygen
Blowing oxygen or oxygen–steam mixture into the bath
Mechanization of slag removal

ELECTROMETALLURGY

Regulators and electrode travel gears in steelmelting furnaces

ROLLING AND TUBE PRODUCTION

Rolling of heavy plates with edging in vertical roll mills
Roll-pass design and rolling of large (no.40) light-weight channel sections
Residual stresses in beams and channels
Use of cooled mandrel bars in a disc-piercing mill

METAL SCIENCE AND HEAT-TREATMENT

Weldability of low-alloy converter steels
Methods of rating the deep-drawing capacity of sheet steel
Calculation of the mechanical strength of low-alloy steels

HEAT TECHNOLOGY

Discussion of some thermotechnical problems in operating large capacity open-hearth furnaces

POWER AND PLANT

Purification of return-water installations for cleaning blast-furnace top gases

No.7

IRONMAKING

Properties and production of pellets made of magnetic concentrates
Automatic control of the blast-furnace process using analogue computers
Smelting low-manganese iron at southern works
Increased life of blast-furnace charging equipment
Modernization of a sintering-machine exhauster

STEELMAKING

Production and properties of BSt5pc Bessemer capped steel
Discussion on the change in the open-hearth performance index
Discussion on the method of determining the productive capacity of open-hearth furnaces

ELECTROMETALLURGY

Alloying of austenitic chrome–manganese steels with nitrogen
Alloying steel with nitrogen using organic agents

ROLLING AND TUBE PRODUCTION

Discussion on the dispatch of rolled products by theoretical weight
Increased life of continuous thin-sheet mill rolls
Specialization of continuous rod and small-section mills
Wall deformation during expansion tube drawing
Activated lubricants for cold rolling stainless-steel tubes
Conference on the theory and practice of drawing and cold rolling of steel tubes

METAL SCIENCE AND HEAT-TREATMENT

Properties of A12A steel deoxidized with aluminium
External crack formation in an ingot during hot deformation
Assessing the quality of alloys for electric heaters
Influence of the rolling process and carbon content on the properties of deep-drawing steel killed with aluminium

ECONOMICS AND ORGANIZATION

Influence of the equipment of open-hearth shops on production indices

FOUNDRY PRACTICE

Influence of the construction of semi-permanent casting boxes on the life of ingot moulds
The position of the joint of the casting box of semi-permanent moulds

POWER AND PLANT

Gas-turbine installations for the utilization of waste heat
Dehydration and utilization of sludges at the gas-cleaning installation of the Kuznetsk combine

NEWS | *Announcements and News of Science and Industry*

THE IRON AND STEEL INSTITUTE

Autumn General Meeting 1959

The Autumn General Meeting of The Iron and Steel Institute will be held in London on Wednesday and Thursday, **2 and 3 December 1959.** The sessions, some of which will run simultaneously, will be held at the Great Hall, Caxton Hall, Caxton Street, London SW1 (Sessions A and B), and the Hoare Memorial Hall, Church House, Great Smith Street, London SW1 (Sessions C, D, E, and F). The papers to be discussed will all have been published in the *Journal* in advance of the meeting, with three exceptions; these will be preprinted and circulated to those attending the meeting.

The following is the detailed programme (month of publication in the *Journal* given in parentheses):

Wednesday, 2 December

(Great Hall, Caxton Hall)

9.30–10 Formal business and election of members.

Session A: Forging

10–11.45 Presentation and discussion of a paper on *Steels for forging* by R. J. Brown (preprint)

11.45–12 Interval

12–1.15 Presentation and discussion of a paper on *Operational research in the forging industry* by R. H. Collcutt, J. Banbury, and J. V. Chelsom (October)

2.30–3.45 Presentation and joint discussion of the following papers:
Theoretical analysis of bloom and billet forging by J. G. Wistreich and A. Shutt (October)
Spread and elongation in flat tool forging by A. Tomlinson and J. D. Stringer (October)

3.45–5.15 Presentation and joint discussion of the following papers:
Heating for forging in a batch-type furnace by P. C. Finlayson and J. S. Schofield (November)
Heat reflectors in forging by A. R. Hardy and J. D. Stringer (November)

Session C: Fuming in steelmaking furnaces

(Hoare Memorial Hall, Church House)

2.15–5.15 Presentation and joint discussion of the following papers:
Preliminary studies on the evolution of fumes at high temperatures by E. T. Turkdogan and L. E. Leake (June)
Factors affecting fuming in open-hearth furnaces by W. Strauss and M. W. Thring (November)
Fuming in open-hearth furnaces by C. Holden (October)
Prevention of brown ferric oxide fume by steam blowing by H. Kosmider, H. Neuhaus, and P. E. Hardt (September)
Some factors affecting fume evolution during oxygen blowing by K. Knaggs and J. M. Slater (November)
A film entitled *Bubbles in steelmaking* will be introduced by Mr B. Larsen of United States Steel Corporation

6.30 Academician G. Kurdjumov, Director of the Department of Metallography and Metal Physics at the Central Research Institute for Ferrous Metallurgy, Moscow, will deliver a lecture on *Martensitic transformations in metals and alloys*

Thursday, 3 December

Session B: Extrusion

(Great Hall, Caxton Hall)

9.30–11 Presentation and discussion of a paper on *The cold extrusion of steel* by R. A. P. Morgan (November)

11–11.15 Interval

11.15–1.0 Presentation of the following papers:
The extrusion of steel by E. K. L. Haffner and J. Séjournet (preprint)
Some aspects of steel extrusion by R. Cox, T. McHugh, and F. A. Kirk (preprint)
The extrusion of steel tubes by J. R. Naden (November)
The extrusion of high-speed steel sections by P. J. Sukolski and G. Hoyle (November)
A film entitled *The hot extrusion of steel* will be shown

2.30–5 General discussion of the operational and engineering aspects of the hot extrusion of steel

Session D: Carbides and nitrides in steels

(Hoare Memorial Hall, Church House)

10–1 Presentation and discussion of the following papers:
Carbides in annealed and quenched high-speed steels by T. Malkiewicz, Z. Bojarski, and J. Foryst (September)
Hexagonal carbide and nitride phases and their occurrence in some alloy steels by K. W. Andrews and H. Hughes (November)
An electron diffraction method for determining the lattices of thin lamellar precipitates in steels by G. R. Booker, J. Norbury, and A. M. Rose (November)

Session E: Controlled-transformation stainless steels

(Hoare Memorial Hall, Church House)

2.30–3.30 Presentation and discussion of a paper on *Controlled-transformation stainless steels* by K. J. Irvine, D. T. Llewelyn, and F. B. Pickering (July)

Session F: Tempering of steels

(Hoare Memorial Hall, Church House)

3.30–5 Presentation and joint discussion of the following papers:
The tempering of 2¼%Cr–1%Mo steel after quenching and normalizing by R. G. Baker and J. Nutting (July)
A resistometric study of the first stage of tempering in plain carbon steels by H. W. King and S. G. Glover (October)
The influence of tempering time on some of the mechanical and physical properties of steel by J. L. Aston (August)

Special Meeting in Italy 1960

At the invitation of the Associazione Italiana di Metallurgia, The Iron and Steel Institute is holding a Special Meeting in Italy from **29 May to 11 June 1960.** The following is an outline of the programme.

The meeting will begin in Milan with an inaugural session at the National Museum of Science and Technology on the morning of 30 May; this will be followed by a series of works visits in the Milan area. Among the works to be visited are those of the Acciaierie e Ferriere Lombarde Falck, Breda Siderurgica, Dalmine, and Innocenti. There will also be visits to FIAT at Turin and to Nazionale Cogne near Aosta.

On 2 June the meeting will move to Rapallo, from where there will be visits to the works of the Cornigliano and SIAC companies near Genoa.

The second part of the meeting will take place in Rome, from 4 to 8 June. The programme for this section will include a visit to the works of the Terni company.

The final section will be held in Naples, and will include a visit to the works of the Ilva Bagnoli company. Alternative programmes will be provided for the final section to enable members to visit Florence and Venice.

Full details of the programme will be announced early next year.

Twelfth Hatfield Memorial Lecture

The Twelfth Hatfield Memorial Lecture is being delivered at 6.30 p.m. on the evening of Wednesday, **25 November,** at the Firth Hall of Sheffield University, by Academician G. Kurdjumov, Director of the Institute of Metallography and Metal Physics at the Central Institute for Ferrous Metallurgy, Moscow. The subject of the lecture will be *The phenomena occurring in steels during quenching and tempering.*

Applications for tickets should be sent to The Registrar, The University, Sheffield 10, and NOT to the Institute.

NEWS OF MEMBERS

Mr **D. C. Abraham** has joined John Summers and Sons Ltd as blast-furnace engineer.

Miss **C. J. Arlidge** has left BISRA to join the staff of the Federation of British Industries.

Mr **R. G. Bhatawadekar** is now metallurgical adviser to the Tata Locomotive and Engineering Co. Ltd, Jamshedpur, India.

Mr **P. Croset** has been elected a director of B.H.D. Engineers Ltd.

Sir **John Duncanson** has left the National Research Development Corporation on the expiry of his period of appointment; he had been a member since the Corporation was established in 1949.

Dr **A. E. El-Mehairy,** on leave of absence from Cairo University, is now on the staff of the Metals Research Laboratory, Carnegie Institute of Technology, Pittsburgh.

Mr **Sven Fornander** has been elected a member of the Swedish Royal Academy of Engineering Sciences.

Mr **B. M. Hardman** was the only successful candidate for The Fellowship of The Institution of Metallurgists in 1958.

Mr **S. R. Howes,** O.B.E., has retired from the board of The United Steel Companies Ltd; he remains managing director of Templeborough Rolling Mills Ltd.

Mr **L. W. Johnson** has retired from the post of assistant manager of the Development and Research Department of the Mond Nickel Co. Ltd.

Mr **R. D. King** is now with Guest Keen Iron and Steel Co. Ltd, East Moors, Cardiff.

Mr **W. H. Lowther** is now lecturer in metallurgy at the Darlington College of Further Education.

Mr **D. Reilly** has left Guest, Keen and Nettlefolds (South Wales) Ltd to become general manager of E. W. Works, Bridgend Trading Estate.

Dr **E. C. Rhodes** is manager of the general division of the Development and Research Department of the Mond Nickel Co. Ltd, recently formed as part of a reorganization of the department.

Mr **J. E. Robson**, M.C., has been appointed to the board of Samuel Osborn and Co. Ltd.

Mr **B. J. Sunter** is now assistant lecturer in metallurgy (grade B) at Kingston Technical College.

Mr **H. J. Tucker**, southern area sales manager for the Electric Resistance Furnace Co. Ltd for the past 11 years, has been appointed service manager for the company.

Mr **C. S. Whitfield** is now foundry manager at the Holwell Works of the Stanton Ironworks Co. Ltd.

AFFILIATED LOCAL SOCIETIES

Newport and District Metallurgical Society

The officers and committee for the 1959–60 session are as follows:

President G. H. Latham, LL.D., J.P.
Hon. Secretary T. G. Grey-Davies
Hon. Treasurer C. J. Morgan, A.I.M.
Committee
H. H. Stanley, A.MET., F.I.M. (*Chairman*)
H. R. Davies, A.I.M.
B. Jones, D.SC., F.R.I.C.
R. A. Owen-Barnett, M.SC.
H. T. Roberts, A.I.M.
Glanville Jones, B.SC., A.I.M.
G. L. Jones, A.I.M.
C. B. Pennington
J. E. Wells, B.SC., F.R.I.C., A.MET., F.I.M.

CONTRIBUTORS TO THE JOURNAL

P. C. Finlayson, B.SC., PH.D.—Scientific Officer, British Iron and Steel Research Association.
Dr Finlayson, who was born in 1933, was educated at Tonbridge School and the Houldsworth School of Applied Science at Leeds University, graduating with an honours degree in gas engineering in 1955. He was subsequently awarded a Ph.D. degree for his work on coal structure and the mechanism of coke formation, the research being sponsored by the British Coal Utilisation Research Association. While at Leeds University, Dr Finlayson was awarded the Hodsman Memorial Travelling Bursary, enabling him to make a study of modern methods of gas production in Western Europe. Since he took up his present post he has been concerned with research into the efficient utilization of the various forms of energy used in the iron and steel industry, particularly with regard to forging practice.

P. C. Finlayson

A. R. Hardy

A. R. Hardy, A.I.M.—Research and Development Branch, UK Atomic Energy Authority, Springfields.

R. A. P. Morgan

J. W. R. Naden

Anthony Russell Hardy was born in Sheffield in 1932 and educated at Firth Park Grammar School. From 1949 to 1959 he was employed on metallurgical and forging research in the Mechanical Working Division of BISRA, apart from a period of service with the RAF from 1952 to 1954. Earlier this year he was awarded the Associateship of The Institution of Metallurgists after a course of study at the Sheffield College of Technology. In his present position, which he took up recently, he is engaged on the development of beryllium and associated metals.

R. A. P. Morgan, O.B.E., M.I.MECH.E., A.M.I.H.V.E.—Superintendent, Royal Ordnance Factory, Birtley, Co. Durham.
Mr Morgan was educated at Northern Polytechnic and the School of Engineering at The Polytechnic, Regent Street. He served a student apprenticeship at the Gnome et Rhône Engineering Company, and then joined the engineering department of J. Lyons and Co. Ltd, becoming chief engineer of Grosvenor House, Park Lane, at the age of 26. He later became consulting engineer to the Union Dominion Trust, Capetown.
In 1939, Mr Morgan joined the Ministry of Supply as assistant to the chief mechanical engineer at the ROF, Birtley; he was successively promoted to assistant manager, manager, and finally superintendent.

J. W. R. Naden, M.B.E., F.I.M.—Chief Metallurgist, Chesterfield Tube Co. Ltd.
John William Rivington Naden was born in 1901 and educated at Sheffield University. He spent twelve years with the research department of Cammell Laird and Co. Ltd, Sheffield, before joining the research department of the English Steel Corporation Ltd. After eight years with English Steel, Mr Naden joined the Chesterfield Tube Co. Ltd in 1939 as chief metallurgist, a position he still occupies.

J. S. Schofield, B.SC. (TECH.), G.I.CHEM. ENG.—Scientific Officer, British Iron and Steel Research Association.
Mr Schofield was born in 1932 and educated at Glossop Grammar School and Sheffield University, where he obtained his degree in the Department of Fuel Technology and Chemical Engineering in 1955. He worked at the Fuel Research Station, Greenwich, on boilers and smoke abatement after graduating, and later became assistant lecturer in chemical engineering at West Ham College of Technology. He joined BISRA in 1957, and has since been concerned with the energy requirements of the iron and steel industry, the use of carbonaceous additions to fuel oil for open-hearth furnace firing, and the design and operation of forging furnaces.

NEWS OF SCIENCE AND INDUSTRY

Max-Planck-Institut für Eisenforschung, Düsseldorf

Professor Dr F. Wever has retired from the post of director of the Max-Planck-Institut für Eisenforschung, Düsseldorf. He has been succeeded by Professor Dr W. Oelsen.

Ore-preparation plant at Stanton

Mr F. Le Neve-Foster, a director of the company, performed the opening ceremony of the new ore-preparation plant of the Stanton Ironworks Co. Ltd on 1 October; he was deputizing for the chairman, Mr A. G. Stewart, who was unable to be present owing to illness.

The new plant lies to the north-west of the works and is designed to process 25000 to 30000 tons of ore per week. Extensive sidings have been built in which the various ores to be blended can be assembled. From these, wagons are fed by gravity to a wagon haul, which delivers them to a tippler rated at thirty wagons per hour. The ore then passes through rough screening feeders to a primary crusher, rated at 800 tons/h, where it is reduced to 6–8 in. In the secondary cone crushers, it is further reduced to 3 in.

The ore is then passed to a bedding plant, which consists of four piles each containing 6000 tons of ore. After reclaiming, the ore is screened, the $-\frac{1}{8}$ in. material going to the sinter plant and the larger fraction direct to the two blast-furnace plants.

The sinter machine is 120 ft long × 8 ft wide, with 15 windboxes; its speed range is 4–15 ft/min, and the rated capacity is 75 tons/h. At the end of the strand, the sinter is cooled in a Lurgi rotary cooler.

The main contractors for the new plant were Mitchell Engineering Ltd (crushing plant), Frazer and Chalmers Ltd and Moxey Ltd (reclaiming and distribution plant), and Huntington Heberlein Ltd (sinter plant). The cost of the plant, which took two and a half years to construct, was in the region of £4 million.

Russian continuous casting dictionary

The latest addition to the series of 'Concise Iron and Steel Dictionaries' published by the British Iron and Steel Federation is a very timely one, since it is a vocabulary of Russian and English terms connected with the continuous casting of steel. Prefacing the vocabulary itself, which contains many standard iron and steelmaking terms in addition to the more specialized ones connected with continuous casting, is a brief history of the development of the process. The interpretation of the terms is helped by the inclusion of several diagrams.

Copies are available free of charge from the Information Officer, British Iron and Steel Federation, Steel House, Tothill Street, London SW1.

Tonnage oxygen plant for Appleby-Frodingham

A tonnage oxygen plant is to be installed at the Scunthorpe works of the Appleby-Frodingham Steel Company at an estimated cost of £750000. The plant is expected to be in commission by the late summer of 1960.

The new plant is being designed to produce 100 tons of gaseous oxygen of 99·5% purity per day at 600 lb/in². Alternatively, 90 tons of gaseous oxygen and 10 tons of liquid oxygen can be produced daily, the latter being stored to provide for planned stoppages of the main plant.

Power-Gas reorganization

The trading activities of the Power-Gas Corporation Ltd have been transferred to two wholly owned subsidiaries. Ashmore, Benson, Pease and Co. Ltd will continue to control the Stockton works, which manufactures heavy engineering products, and will specialize in the design and manufacture of plant and equipment for the iron and steel industry. P.G. Engineering Ltd, a newly formed subsidiary, will undertake the design, supply, and erection of plant and equipment for the chemical, petroleum, petrochemical, gas nuclear energy, and other industries.

Appleby-Frodingham to produce universal beams

The Frodingham section mills of the Appleby-Frodingham Steel Company are to be extended by the addition of equipment to produce universal beams, I sections with parallel edges, and column sections. A contract has been placed with Schloemann AG of Düsseldorf, but the equipment will be made in the UK by the Distington Engineering Co. Ltd, another United Steel subsidiary. The new extensions should be in operation by the autumn of 1961.

New publications

Two new publications of interest to members of the Institute have recently appeared. The first, in newspaper format, comes from the British Safety Council and is known as *Safe Times*. In a message published in the first issue, dated September, the Minister of Labour defines the role of the new publication as a source of 'fresh guidance and inspiration in dealing with the many difficult problems of safety and accident prevention'.

The Scrap Reclaimer is a weekly journal devoted entirely to the interest of the ferrous and non-ferrous scrap metals industry. It costs 9*d*. and may be obtained from 1 Eardemont Close, Crayford, Kent.

FORTHCOMING CONFERENCES AND EXHIBITIONS

Building Exhibition

The next Building Exhibition will be held at Olympia, London, from 18 November to 2 December; it is to be opened by the Minister of Works.

Hydrometallurgy symposium

The Australasian Institute of Mining and Metallurgy is holding a symposium on the wet processing of minerals and industrial products from 16 to 19 February 1960 in Adelaide.

Reliability of digital computer systems

'Managerial and engineering aspects of reliability and maintenance of digital computer systems' is the title of a series of discussion meetings to be held on 20 and 21 January at the Institution of Electrical Engineers, Savoy Place, London WC2. The meetings are being held under the aegis of the British Conference on Automation and Computation.

Production Exhibition

The 4th Production Exhibition will take place next year at the National Hall, Olympia, London, from 25 to 30 April. A series of conferences to be organized by the British Productivity Council with the title 'Productivity, men and methods' will be held during the Exhibition. Enquiries should be sent to the organizers at 11 Manchester Square, London W1.

Corrosion Exhibition

A measure of the success of the 1959 Corrosion Exhibition, sponsored by the journal *Corrosion Technology*, is that it is moving, in 1960, from the Royal Horticultural Halls to Olympia. The whole of the Empire Hall has been taken for the period 29 November to 2 December. Enquiries should be sent to the organizers at Leonard Hill House, Eden Street, London NW1.

INDUSTRIAL PUBLICATIONS RECEIVED

Stainless Steel in the Dairy Industry is the title of an attractive 24-page illustrated brochure from the Stainless Steel Manufacturers' Association.

An alphabetical list of products has been published by the General Chemicals Department of Albright and Wilson (Mfg) Ltd.

The latest of the foundry leaflets from the Incandescent Heat Co. Ltd cover hot blast (FP.3) and ladles and receivers (FP.4).

List LS 2700 from the Lancashire Dynamo Group deals with motor control gear, automatic direct-on-line and star-delta types.

DIARY

1–6 Nov. AMERICAN SOCIETY FOR METALS—41st National Metal Congress and Exposition—Chicago, Ill., USA.

4 Nov. BRITISH NUCLEAR ENERGY CONFERENCE—*The design and testing of large gas ducts*, by A. T. Bowden and J. C. Drumm—Institution of Mechanical Engineers, 1 Birdcage Walk, London SW1, 6 pm.

4 Nov. SWANSEA AND DISTRICT METALLURGICAL SOCIETY—*Fabrication by powder metallurgy*, by Dr I. Jenkins—Central Library, Swansea, 6.45 pm.

4–6 Nov. VEREIN DEUTSCHER EISENHÜTTENLEUTE — Eisenhüttentag — Düsseldorf.

5 Nov. LIVERPOOL METALLURGICAL SOCIETY—*Ductile fracture*, by Professor R. W. K Honeycombe—Department of Metallurgy, Liverpool University, 7 pm.

5 Nov. NEWPORT AND DISTRICT METALLURGICAL SOCIETY—*Basic oxygen steelmaking processes*, by Dr. T. P. Colclough, C.B.E.—Whitehead Institute, Newport, 7 pm.

11 Nov. LIVERPOOL METALLURGICAL SOCIETY—*Welding in warship construction*, by W. R. Seward—Picton Library, Liverpool, 7.30 pm.

11 Nov. MANCHESTER METALLURGICAL SOCIETY—*Bearing metals*, by P. G. Forrester—Central Library, Manchester, 6.30 pm.

11 Nov. NORTH WALES METALLURGICAL SOCIETY—*Recent developments in plain bearings*, by P. P. Love—Flintshire Technical College, Connah's Quay, 7 pm.

12 Nov. INSTITUTE OF METALS—Symposium on *The application of thin-film techniques to the electron-microscopical examination of metals*—Royal Institution, Albemarle Street, London W1, 9.30 am.

18 Nov. BRITISH NUCLEAR ENERGY CONFERENCE—Informal discussion on nuclear power plant—Institution of Mechanical Engineers, 1 Birdcage Walk, London SW1, 6 pm.

20 Nov. WEST OF SCOTLAND IRON AND STEEL INSTITUTE — *Metallurgical aspects of flat rolled products*, by A. J. K. Honeyman—39 Elmbank Crescent, Glasgow, 6.45 pm.

24 Nov. BRITISH NUCLEAR ENERGY CONFERENCE—Symposium on *Nuclear energy*—Royal Institution, Albemarle Street, London W1, 2.30 pm.

25 Nov. BRITISH NUCLEAR ENERGY CONFERENCE — Thomas Hawksley Lecture: *The effect of nuclear radiation on engineering materials*, by Professor A. H. Cottrell—Institution of Mechanical Engineers, 1 Birdcage Walk, London SW1, 6 pm.

25 Nov. THE IRON AND STEEL INSTITUTE—12th Hatfield Memorial Lecture: *The phenomena occurring in steels during quenching and tempering*, by Academician G. Kurdjumov—Firth Hall, Sheffield University.

25 Nov. MANCHESTER METALLURGICAL SOCIETY—*Engineering aspects of civil nuclear power stations*, by B. D. Willson—Central Library, Manchester, 6.30 pm.

25–27 Nov. SOCIETY OF CHEMICAL INDUSTRY (Corrosion and Chemical Engineering Groups) and INSTITUTE OF PETROLEUM—Symposium on *Corrosion problems of the petroleum industry*—London.

1–5 Dec. INDIAN INSTITUTE OF METALS—13th Annual Technical Meeting—Bangalore.

2–4 Dec. THE IRON AND STEEL INSTITUTE—Autumn General Meeting—London.

2 Dec. SWANSEA AND DISTRICT METALLURGICAL SOCIETY—*Alternatives to the blast-furnace*, by J. M. Ridgion—Central Library, Swansea, 6.45 pm.

3 Dec. LIVERPOOL METALLURGICAL SOCIETY—*Basic aspects of metal fatigue*, by Dr T. Broom—Department of Metallurgy, Liverpool University, 7 pm.

9 Dec. MANCHESTER METALLURGICAL SOCIETY—*Guided missiles: metallurgical problems as seen by the designer*, by J. L. Crowder—Central Library, Manchester, 6.30 pm.

9 Dec. NORTH WALES METALLURGICAL SOCIETY—*Deep drawing*, by D. H. Lloyd — Flintshire Technical College, Connah's Quay, 7pm.

15 Dec. BRITISH NUCLEAR ENERGY CONFERENCE—*The shielding and security of marine reactors*, by K. Maddocks—Institute of Marine Engineers, 76 Mark Lane, London EC3, 5.30 pm.

15 Dec. NEWPORT AND DISTRICT METALLURGICAL SOCIETY—*The basic open-hearth, present and future*, by R. W. Evans—Whitehead Institute, Newport, 7 pm.

15 Dec. SHEFFIELD METALLURGICAL ASSOCIATION AND THE IRON AND STEEL INSTITUTE JOINT MEETING—*Developments in heavy plate steels*, by Mr W. Barr, O.B.E., President of The Iron and Steel Institute—Mappin Hall, University, St. Georges Square, Sheffield, 7 pm.

BRITISH IRON AND STEEL INDUSTRY TRANSLATION SERVICE

The following translations are now available, in addition to those given on page 184 of the October 1959 issue of the *Journal*:

New Methods of Plastic Deformation in the Working of Metals.
PLATZER, F., *Berg- u. Hüttenmänn. Monatsh.*, 1957, Apr., pp.115–125. (£6) [**918**

Torsion-resistant Box Girders in Steelworks Crane Construction.
SCHWARZ, H., *Stahl u. Eisen*, 1957, Jan. 10, pp.24–32. (£3 15*s*. 0*d*.) [**923**

Aerodynamic Design of the Open-hearth Furnace. II—Features of Flow in Typical Designs.
HASIMOTO, H. A., *Tetsu-to-Hagane*, 1956, Dec., pp.1102–1105. (£6) [**1121**

Aerodynamic Design of the Open-hearth Furnace. IV—Function of the Furnace Chamber.
HASIMOTO, H. A., *Tetsu-to-Hagane*, 1957, June, pp.628–632. (£7 10*s*. 0*d*.) [**1123**

An Explanation of Interference Errors Occurring Systematically in X-ray-fluorescence-spectroscopic Measurements and Means whereby these Interference Errors may be eliminated.
EBERT, F., and WAGNER, A., *Z. Metallk.*, 1957, Dec., pp.646–649. (£2 15*s*. 0*d*.) [**1177**

Counter-tube Technique with a Newly-developed Specimen Holder for the Representation of Textures in Quantitative Pole-figures.
GREWEN, J., *et al.*, *Arch. Eisenhütt.*, 1958, Feb., pp.115–118. (£3 5*s*. 0*d*.) [**1196**

Experience with Multiple-strand Semi-continuous Casting.
BAAKE, R., and ROSAHL, H., *Neue Hütte*, 1958, Dec., pp.740–746. (£3 10*s*. 0*d*.) [**1224**

Growth and Morphology of Oxide Films on Iron at High Temperatures (with Discussion).
MOREAU, J., and CAGNET, M., *Rev. Mét.*, 1958, Nov., pp.1091–1109. (£7 10*s*. 0*d*.) [**1225**

The Desulphurization and Deoxidation of Pure Iron in the Vacuum Melting Process.
JANICHE, W., and BECK, H., *Arch. Eisenhütt.*, 1958, Oct., pp.631–642. (£5 15*s*. 0*d*.) [**1227**

Jet Tapping of Open-hearth Furnaces. (Abridged.)
PRIOR, J. I—Method of Construction and Operation.
BURMEISTER, W., II—Experience gained, *Stahl u. Eisen*, 1957, May 2, pp.562–567. (£2 10*s*. 0*d*.) [**1234**

Effect of Carbon, Arsenic, Copper and Tin on Transition Temperature of Steel.
SAWAMURA, H., *et al.*, *Tetsu to Hagane*, 1955, Aug., pp.861–869. (£9 10*s*. 0*d*.) [**1240**

The Intensity of Deformation during Rolling.
WUSATOWSKI, Z., *Prace Inst. Hutn.* (Poland), 1958, (2), pp.120–123. (£2); *Neue Hütte*, 1958, Aug., pp.494–497. [**1259**

Contribution to the Dynamics of Steel-making Processes.
HEISCHKEIL, W., and KOOTZ, T., *Stahl u. Eisen*, 1959, Feb. 19, pp.205–210. (£4 5*s*. 0*d*.) [**1265**

The Cold Hobbing of Forging Dies.
SCHIMZ, K., *Industrie-Anzeiger*, 1957, vol. 79, Oct. 8, (81), pp.1217–20 (£4 10*s*. 0*d*.) [**1280**

Causes and Prevention of Surface Fissures of Hot-worked Steel Products. I—Influence of Heating Conditions, Scaling Loss and Tramp Elements on the Surface Fissures.
OHTAKE, T., *et al.*, *Nippon Kinzoku Gakkai-Si*, 1957, Sept., pp.536–540. (£9 10*s*. 0*d*.) [**1316**

Electro-hydraulic Forging Press with Crank Drive Apparatus.
HERTL, A., *Stahl u. Eisen*, 1959, Apr. 16, pp.477–485. (£6 5*s*. 0*d*.) [**1318**

Under-driven Forging Presses (with Discussion).
HOWAHR, E., and PAHNKE, H. J., *Stahl u. Eisen*, 1959, Apr. 16, pp.485–494. (£6 15*s*. 0*d*.) [**1321**

Drives for Forging Presses.
RIEMENSCHNEIDER, F., and NICKRAWIETZ, K., *Stahl. u. Eisen*, 1959, Apr. 16, pp.494–500. (£4 10*s*. 0*d*.) [**1322**

Hydraulic Press Control.
DOHRN, W., and ROBRA, H., *Stahl u. Eisen*, 1959, Apr. 16, pp.500–514. (£8 10*s*. 0*d*.) [**1323**

Consideration of the Chemical Attack on Refractories by Steel Baths during Metallurgical Processes.
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Spectrographic Analysis of High-Silicon Aluminium Alloy and Welds.
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The Possibilities of Quantitative Autoradiography by Means of Microphotometric Measurements.
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The Uses of the Electronic-probe Microanalyser in Ferro-metallurgical Research.
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Trends in the Development of Automation.
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Experience and Knowledge Gained from the Operation of a Hot Wide-strip Mill and a Cold Strip Mill.
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Design of the Screwed Joint of Crane Hooks in Correct relation to the Stresses Encountered.
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Investigations of Bessemer Converter Bottoms.
KÖHLER, W., *et al.*, *Stahl u. Eisen*, 1959, May 14, pp.674–683. (£6 5*s*. 0*d*.) [**1369**

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Heat Transfer of Rolled Finned Tubes.
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Systematic Examination of the Heat Transfer and Resistance to Flow of Finned Tubes.
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The Prevention of Gassing Accidents in Iron and Steelworks.
ZILLMER, E., *Stahl u. Eisen*, 1959, vol.79, Apr. 30, pp.629–634. (£4 5*s*. 0*d*.) [**1385**

On the Determination of the Tensile Strength of Tubes in Ring Tensile Tests.
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The Significance of the Corrosion Processes in Aqueous Hydrogen Sulphide Solutions for the Formation of Cracks in Steels, Part II. (See also nos.1373 and 1413.)
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Limitations in the Evaluation of Gamma Radiographs used for Examining Circumferential Pipe Welds in Accordance with DIN 54 111.
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Slag Explosions in open-hearth Steelworks.
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On the Mechanism of Internal Friction Peak of Interstitial Atoms in the Face Centre Cubic Crystal (from Chinese).
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The Activation Energy of the Process of Diffusion Creep in Disordered Substitutional Solid Solutions of Metals.
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Liquid honing.
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HORSTMANN, D., *Stahl u. Eisen*, 1957, Mar. 21, pp.359–362; Apr. 4, pp. 435–438; Apr. 18, pp.512–525. (£6) [**1404**

Arc Surfacing of Components Subject to Abrasion with Hard Alloys.
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Development Tendencies and Weldability of Modern Steels for Boilers, Container and Chemical Plant Construction.
ZEYEN, K. L., *Ind. Anzeiger*, 1958, June 27, pp.739–743. (£5) [**1406**

Influence of Photo-synthesis on the Corrosion of Steel in Sea-water.
HACHE, A., *et al.*, *Corrosion et Anticorrosion*, 1959, Feb., pp.56–62. (£3 5*s*. 0*d*.) [**1407**

A Comparison between Low-carbon Austenitic Stainless Steels and Stabilized Steels for Welded Apparatus in the Chemical Industry.
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Development of a Welded Connexion between Ferritic and Austenitic Superheater Tubes for Steam Power Plants.
LÖFBLAD, B., and LINDH, G., *Internat. Inst. of Welding*, 1958, June 30, Vienna. (£4) [**1409**

The Present State of Knowledge concerning the Sigma Phase in Iron-Nickel-Chromium Alloys (with bibliography: 60 references).
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The Behaviour at the Yield-point of Skin-passed Deep-drawing Steel Sheets after Ageing.
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The absorption of hydrogen from aqueous hydrogen sulphide solutions by steel; and the formation of cracks in steels in tension. Part III (with Discussion). (See also nos.1373 and 1390.)
NAUMANN, F. K., and CARIUS, W., *Arch. Eisenhüt.*, 1959, June, pp.361–370. (£7) [**1413**

The Appearance of Fractures of Tubular Test Pieces after Internal Pressure. Tests at Temperatures to −70°C (with Discussion).
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Means of Eliminating Stoppages in Sinter Plants. (Summary.)
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Heat-resisting Ferritic Steels.
BETTZIECHE, P., *Vereinigung Grosskesselbesitzer, Mitt.*, 1958, Dec., (57), pp.393–397. (£3 5*s*. 0*d*.) [**1420**

Thermal shock tests on austenite-ferrite connectors.
SCHINN, R., and RUTTMANN, W., *V.G.B., Mitt.*, 1958, Dec., (57), pp.407–411. (£3) [**1422**

Simplified Method for Testing the Piercing Properties of Metals.
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The Influence of Charging Times and Storage at Room Temperature on the Hydrogen Embrittlement of Steels.
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WIESTER, H.-J., *et al.*, *Stahl u. Eisen*, 1959, Aug. 6, pp.1120–1129. (£5 5*s*. 0*d*.) [**1431**

Fatigue tests on parent metals and buttwelds.
KOLLMAR, A., *Stahlbau*, 1956, Sept., pp.205–210. (£3 15*s*. 0*d*.) [**1432**

The physico-chemical basis of desulphurisation in the basic open-hearth furnace (with Discussion).
SPEITH, K. G., *et al.*, *Stahl u. Eisen*, 1959, June 25, pp.926–933. (£4 15*s*. 0*d*.) [**1434**

Determination and separation of the carbon monoxide decomposition, the hydrate water and carbonate decompositions and the hydrogen gas reaction during reduction experiments on iron ores and sinters.
SCHÜRMANN, E., *et al.*, *Arch. Eisenhüt.*, 1959, June, pp.321–327. (£5 10*s*. 0*d*.) [**1453**

ABSTRACTS OF CURRENT LITERATURE *Iron and Steel Manufacture and Related Subjects* AND BOOK NOTICES

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MINERAL RESOURCES

The world's iron ore supplies E. G. Percival (*British Iron and Steel Federation, pamphlet*, 1959, Jan. (revision), pp.19) Sources are reviewed.

The general world iron-ore situation C. R. Wheeler (*JISI*, 1959, **193,** Nov., 209–210) [This issue].

Canadian iron ore and the North American iron and steel industry (*Dept. Mines Techn. Surveys, Ottawa, Min. Res. Division, map* MRI) A map taking in N., Central, and part of S. America.

Truro map-area, Colchester and Hants Counties, Nova Scotia I. M. Stevenson (*Geol. Survey Canada*, Memoir 297, 1958, pp.124) Iron ore deposits are included (90–93).

Reconnaissance of Nevada manganese deposits R. R. Trengove (*US Bur. Mines Rep. Invest.*, 5446, 1959, pp.40).

On the geochemical prospecting of manganese by analysis of soil K. Takimoto, T. Okuda, and N. Hikotani (*Suiyokwai-Shi*, 1958, **13,** Dec., 669) Studies in the vicinity of two mines are described.

Core-drill sampling of Cuyuna-range manganiferous iron formations, Crow Wing County, Minn. L. F. Heising, R. L. Marovelli, P. A. Wasson, S. R. B. Cooke, and J. W. Pennington (*US Bur. Mines Rep. Invest.*, 5450, 1959, pp.34).

The composition and microtexture of an Ulvöspinel magnetite intergrowth E. H. Nickel (*Dept. of Mines and Tech. Surveys, Ottawa, Res. Rep.* R27, from *Canadian Mineralogist*, 1958, **6,** (11); 1958, Sept. 23, 191–199) An intergrowth in titaniferous ore is examined and composition, structure, and texture are determined.

On the TiO_2- content of magnetites as a petrogenetic hint V. Marmo (*Am. J. Sci.*, 1959, **257,** Feb., 144–149) The relationships to the host rocks are considered.

ORES-MINING AND TREATMENT

Canadian iron ore P. E. Cavanagh (*AIMME Blast Furn. Comm. Proc.*, 1958, **17,** 275–282) Some statistics and compositions are given and reducibility testing is considered.

The BHP-Al & S Ltd research programme H. K. Worner (*BHP Techn. Bull.*, 1959, **3,** Mar., 3–9) Ore dressing, coal and coke research, sintering of Australian haematite, and other work in hand are considered.

The ore-carrier 'Morar' (*Shipbuilder*, 1959, **66,** Mar., 141–151) An illustrated account of the new gas-turbine driven ore-carrier is given.

Economy of designing ore-dressing plants B. Soukup (*Czech. Heavy Ind.*, 1959, (4), 34–40) Iron ore dressing is described with details of costs and curves showing various optimum conditions and the necessary calculations are indicated.

Application of continuous filters in the metallurgical flow-sheet D. A. Dahlstrom (*Can. Min. Met. Bull.*, 1959, **52,** Feb., 94–111) A review of the types of filter, theory of filtration, precoat materials, cake washing, de-watering rate and drying with heat and their significance in ore and coal treatment.

New methods of preparation of Ilsede iron ore E. Meinecke (*Stahl Eisen*, 1959, **79,** May 14, 657–668) The Ilsede iron ore now found is very much lower in its iron content, with higher P and Mn contents, than it was when winning started. The means by which the iron content can be raised and the Mn level reduced are discussed and a plant is described for burden preparation. A 20% coke saving is expected from the use of the prepared burden, the iron content of which has been raised from about 27% to about 40% (22 refs).—T.G.

Preliminary report on the results of X-ray investigation of the decomposition of manganese ores R. Rotter and D. Kašparova (*Sbornik vyzkumnych praci ustavu pro Vyzkum Rud*, 1956, 125–129) The isomorphism of Fe and Mn in oligonite and the determination of the ratio Fe : Mn by X-ray means is considered and the method is confirmed on synthetic materials. The thermal decomposition of the ore concentrate is then examined similarly.

Extraction of phosphorus from manganese concentrates V. Horak (131–136) Acid extraction of the phosphatic gangue is investigated on a roasted carbonate ore. About 95% of the P could be removed after calcining in an oxidizing atmosphere up to 1000°C for 45 min by extraction with HCl or HNO_3, the consumption of acid being about 10% greater than the theoretical. Above 50° extraction is more effective. HNO_3 is recommended when Mn can be increased from 20·1 to 36·59% and P decreased from 3·53 to 0·054%.

Dressing of dolomitic magnesite V. Popov, K. Sobotka, and K. Skala (137–146) A heavy liquid separation and flotation process and the production of 86–88% MgO bricks is described.

Operating experience with a covered ore-fines stockyard P. A. Gorbunov and A. S. Sazonov (*Stal'*, 1959, (2), 106–109) The stockyard is at a sinter plant at Magnitogorsk. It is found that by mixing, proportioning, and removing ore in a fixed sequence much greater uniformity can be secured. Effectiveness of mixing increases with bay length. The number of mobile bunkers should correspond to the number of components received with a reserve and there must be at least two bays for each ore component.

Contribution to the assessment of the smelting qualities of iron ores and sinter P. Dickens, W. vor dem Esche, and J. Willems (*Stahl Eisen*, 1959, **79,** June 25, 905–917) This is a continuation of previous work by the same authors (*ibid.*, 1956, **76,** 1404). Iron ore was examined in the hot-stage microscope for softening behaviour under pressure, this behaviour was also tested on macro-cubes and cylinders of ore. The reduction behaviour in flowing gases was studied and in connection therewith microscopical analysis was carried out. A scheme for ore appraisal is suggested.

Reduction trials on iron ore and sinter, taking fully into account the formation of dissociation carbon and the evolution of carbon dioxide and crystal water E. Schürmann, G. Heynert, and H. Beer (*Stahl Eisen*, 1959, **79,** June 25, 917–926) This is a preliminary report on the quantitative evaluation of reduction trials of iron ore and sinter, dealing mainly with principles involved and the factors that may affect the reduction. Ore or sinter of a fixed grain-size are reduced by a mixture of CO, H_2, and N_2 during heating from 100° to 1100°C at a rate of 4°C/min.

New iron ore sintering plants and associated problems (*Ind. Heat.*, 1959, **26,** Mar., 526–530, 566) Brief details are given of the design and operation of the sintering plants at Inland Steel Co., Armco Steel Co.'s Ashland Works, Weirton Steel Co., and the Cleveland Works of Jones and Laughlin Steel Corp. The problems and features peculiar to each installation are mentioned.—G.F.

Sinter plant at Ashland (*Brit. Steel.*, 1959, **25,** Mar., 92–93) The 2400-t/day sinter plant at the Ashland, Kentucky plant of Armco Steel Corporation is described. The plant was constructed by the Dravo Corporation of Pitts-

burgh under licence from the Lurgi Gesellschaft für Chemie und Hüttenwesen of Germany.

Fuels for sintering T. L. Myron, M. R. Faigen, and R. L. Franklin (*AIMME Blast. Furn. Comm. Proc.*, 1958, **17**, 282–293) In small scale and continuous trials the highest production rates were obtained with by-product coke breeze, stoker coke, anthracites, anthrafines, Alpheus coal middlings, and beehive coke. Anthrafines are a blend of no.4 and no.5 anthracites.

Sintering of high-grade haematite ore with special reference to lime additions and reducibility E. Klein (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 265–275) Work at ISCOR on the effect on blast furnace production of the use of sinter prepared from the screenings below ¼ in. is described. Beneficiation, improved screening, and use of limestone as flux produced improvements. A low FeO content appears to be important.

Sinter cooling: an important part of modern sinter plants G. Brandes, H. Wendeborn, and F. M. Hamilton (*AIMME Blast Furn. Comm. Proc.*, 1958. **17**l 39–46) A cooler is described in which air is sucked through a horizontal or inclined low bed. This is said to make transfer to the furnaces easier and to improve sinter quality. Various water-cooling methods are reviewed.

A review of self-fluxing sinter applications T. E. Ban (*Blast Furn. Steel Plant*, 1959, **47**, Mar., 294–302) Production of self-fluxing sinter by the Dwight–Lloyd process is discussed, with reference to the literature (42 refs).

Iron coke process H. Barking and C. Eymann (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 294–303) An account of the process as worked by Bergwerkgesellschaft Walsum and of the product manufactured.

FUEL—PREPARATION, PROPERTIES AND USES

Coal-dressing plants from the research viewpoint F. Špetl (*Czech. Heavy Ind.*, 1959, (4), 3–5) Processes in use and the directions of endeavours to improve them are reviewed. **New trends in designing coal-dressing plants** J. Chleboun (6–11). **Some coal beneficiation installations** R. Moreš(12–17) The use of Škoda equipment is especially considered. **Modern technology of coal beneficiation** J. Maly (29–32).

Present trends in the preparation of coal C. Bertrand y Bertrand (*Inst. Nac. Carbon Bol. Inf.*, 1958, **7**, Nov.–Dec., 279–290) A review based on the proceedings of the 3rd International Conference at Liége. Dense-medium processes are favoured, the Baum jig with deshaler or the centrifugal cyclone. Methods in use in Spain are specially reviewed.

Use of statistics in coal processing W. M. Bertholf (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 91–114) Large numbers of plots of variations in coal compositions and the use that might be made of them are considered.

A method for evaluating the plastic properties of highly swelling coals J. Bandopadhyay, S. Sarkar and N. N. Das Gupta (*J. Sci. Ind Res.*, 1959, **18B**, Mar., 116–118).

Advantages obtained in the coking of coal by the addition of mineral oil M. Ayban Gallego (*Inst. Nac. Carbon Bol. Inf.*, 1958, **7**, Nov.–Dec., 243–253).

Choice of constituents of a coke blend R. Loison and P. Foch (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 170–192) In France high-volatile bituminous are available which require mixing with low- or medium-volatile types or inerts. Observations at Marienau and their correlations with laboratory studies at CERCHAR are discussed.

Use of a thirty-pound test oven for rapidly assaying the coking strength of coals J. G. Price, R. W. Shoenberger and B. Perlic (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 212–225).

New coke research centre British Coke Research Association (*Iron Coal Trades Rev.*, 1959, **178**, Apr. 10, 839–842) An illustrated account of the new Chesterfield plant.

The recently developed Burstlein-Longwy process for coke production from bituminous and non-bituminous coals E. Burstlein (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 66–78) Advantages claimed for the process and a list of installations are given.

An introduction to Durgapur coke-ovens A. B. Ganguli (*Iron Steel Rev.*, 1959, **2**, Mar., 935–937) An account of the novel features of the plant.

Carbonizing tests with Tuscaloosa oven: studies of oven width flue temperature and coking rate J. B. Gayle and W. H. Eddy (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 193–212).

A more productive coke oven – speculative thoughts on its future size J. S. Mohr jun. (*Blast Furn. Steel Plant*, 1959, **47**, Mar., 277–280) Factors affecting the possibility of increasing the dimensions of the coke oven are discussed, with reference to the literature. It is concluded that much larger capacities are possible than are available at present, with correspondingly lower costs and higher productivity.

Standardisation of heating practice and control of a coke oven battery G. V. Mosyakov (**Koks i Khimiya*, 1958, (7), 26–29) The factors considered, draught, waste gas volume, pressure differences in the system, and excess air in the flues, are evaluated and regulation of these factors is discussed. The effects of automatic regulation of gas and air supply in accordance with pressure drop in the regenerators between ascending and descending streams are set out and the advantages of the method are indicated. Temperature differences in the gas and air streams should also be taken into account.

New coke-ovens battery at BHP Steel Works, Newcastle A. Cornish (*Austral. Eng.*, 1958, **50**, July 7, 45–47) A new battery of 'Wilputte' underjet regenerative coke ovens is in operation at the Newcastle (N.S.W.) work of Broken Hill Prop. Co. Ltd. It consists of 57 individual chambers, and details are given of the design and operation.—G.F.

Some practical aspects of the degradation of blast furnace coke during handling J. G. Price and J. D. Clendenin (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 79–91) A trial with 10 experimental cokes was made using various coals and blends of U.S. origin. Shatter tests were carried out and the value of increased coke strength and the economics of practices for its improvement are discussed.

Pilot plant research into methods of improving blast furnace coke quality J. A. Gregory (*BHP Techn. Rev.*, 1959, **3**, Mar., 23–28) The laboratory and its equipment are described including the experimental coke oven. Coking tests are reported. Coke strength was increased by addition to the charge of clay shales containing montmorillonite. Washing improves shatter index and reduces ash. The work is still in a preliminary stage.

Metallurgical coke: the problem of coke in Spain (*Inst. Nac. Carbon Bol. Inf.*, 1958, **7**, Nov.–Dec., 254–278) The outlook for the period 1958–1962 is considered in relation to probable output of coking coal and blast furnace and other requirements. Mainly statistical.

Fall and compression behaviour of coke H. Schenck and H. Esch (*Stahl Eisen*, 1959, **79**, May 28, 769–777) The effect of fall and compression on classified coke of various origins was studied and the changes in size, quantity of fine grain produced and grain-size distribution were measured. An attempt was made to find a correlation between the effect of falling and of crushing, but the effects are so different that no relation of any value to blast-furnace practice can be given (52 refs.).—T.G.

Fuel in steelworks O. Hirston (*Iron Steel*, 1959, **32**, Mar., 89–95) Factors influencing the choice of fuel are considered, with particular reference to the manufacture of alloy steels and forgings.

By-product Session (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 226–255). **Wilputte centrifugal extractor phenol plant** R. O. Bowman (226–237). **Disposal of ammonia in coke plants** E. V. Schulte (237–247). **Current market conditions in the coal-tar chemical industry** T. J. Kinsella (247–255).

AIR POLLUTION AND SMOKE

Air conditioning essential for workers' comfort, product quality F. C. Frean (*Ind. Fin.*, 1959, **11**, Apr., 25–26, 56) Air conditioning involves the production and automatic maintenance of suitable temperature, humidity, air movement, and standard of purification within an enclosure, irrespective of fluctuating external conditions. The author discusses these basic essentials and describes typical equipment.—G.F.

Efficient ventilation aids quality and output (*Ind. Fin.*, 1959, **11**, Apr., 28–29) Some of the plant, techniques and services available for air conditioning and dust and fume extraction are discussed, from the points of view of maintaining a healthy working atmosphere and as an essential for efficient production.—G.F.

Dust and fume problems in the finishing industry J. B. Stribling (*Ind. Fin.*, 1959, **11**, Apr., 21–24) The author presents the basic facts of the dust and fume problems encountered in industrial finishing processes. The principles of some of the different types of extraction and collection equipment are outlined.—G.F.

Trouble shooting mechanical dust collectors W. E. Archer (*Blast Furn. Steel Plant*, 1959, **47**, Feb., 192–198) Careful inspection and immediate correction of adverse conditions are required to ensure continued efficient operation of mechanical dust collectors.—T.G.

Electric furnace precipitator J. L. Venturini (*Blast Furn. Steel Plant.*, 1959, **47**, Apr., 371–375, 383) The installation and performance of a high-efficiency electrostatic precipitator are described and illustrated.

Studies on a synthetic iron oxide hydrate for desulphurization of industrial gases B. K. Banerjee, N. G. Basak and A. Lahiri (*J. Sci. Ind. Res.*, 1959, **18B**, Feb., 70–72).

TEMPERATURE MEASUREMENT AND CONTROL

Pyrometry W. Tauss (*Austral. Eng.*, 1958, **50**, July 7, 63–65) The author presents a brief historical review of the development of pyrometry. He then describes four main types of pyrometer, namely the optical pyrometer, the resistance pyrometer, the thermocouple, and the radiation pyrometer.—G.F.

New radiation pyrometer (*Metallurgia*, 1959, **59**, Feb., 86–87) The pyrometer developed by the Wembley Research Laboratories of G.E.C. is described. The instrument, intended for monitoring surface temperature in an induction hardening process, has a thermopile substantially independent of ambient temperature. Its construction and application are outlined.

A graphite/graphite thermocouple for high temperatures A. R. Ubbelohde, L. C. F. Blackman and P. H. Dundas (*Chem. Ind.*, 1959, May 9, 595–596) Graphites of different crystal orientations can be used as thermocouple elements. About 32 mV was recorded at 2 200°C.

REFRACTORY MATERIALS

The role of refractories in an expanding technology J. White (*Refract. J.*, 1959, **35**, Mar., 104–114) Production, properties and recent uses, such as rocket motors and nuclear power plants are reviewed.

A new look at specialized refractories R. W. Brown (*Mech. Eng.*, 1959, **81**, Mar., 58–62) A review of the other properties of super-refractories besides heat resistance such as thermal conductivity, abrasion resistance, thermal shock and impact resistance and chemical inertness is given with design criteria and notes on uses and maintenance.

Refractories for by-product coke ovens E. C. Hartman (*Blast Furn. Steel Plant*, 1959, **47**, Mar., 305, 335–338) Topics discussed include standardised brick shapes and oven designs and their advantages, and the types of brick used.

The liquid phase in firebricks A. K. Bose and H. E. Schwiete (*Refract. J.*, 1959, **35**, Feb., 58–97; May, 170–174, 178) Mullite is the main crystalline phase in firebricks. An analysis of the vitreous phase and the determinations of

mullite and matrix, and the correlation of compositions with firing conditions are examined. The effects of Fe_2O_3 and alkalis and experiments on the synthesis of the liquid phase are considered and the softening temperatures of a series of such glasses were measured. Further work is suggested (39 refs).

Recent work on chrome–magnesite brick J. Laming (*Refract. J.*, 1959, **35**, Mar., 116–118) Tests on volume stability and the changes occurring during firing have been investigated.

The effect of additions on the sintering and dead-burning of magnesia H. J. S. Krick, W. F. Ford and J. White (*Trans. Brit. Ceram. Soc.*, 1959, **58**, Jan., 1–34).

Temperature of inversion in cristobalite R. F. Walker, S. Zerfoss, S. F. Holley and L. J. Gross (*J. Res. N.B.S.*, 1958, **61**, Oct., 251–261).

Thermal expansion of Al_2O_3, BeO, MgO, B_4C, SiC and TiC above 1000°C C. J. Engberg and E. H. Zehms (*NAA–SR–3086*, 1958, Nov. 30, pp. 25; from *Nuclear Sci. Abs.*, 1959, **13**, Mar. 31, 501) Measurements were made up to sintering or permanent deformation temperatures. The coefficient was constant over the range measured but did not extrapolate to room temp. Samples were bent, cracked, enlarged and changed in colour after the tests. The m.p. of BeO was 2450 ±30°C (hot pressed) and 2470 ±20°C (slip-cast).

Note on the system magnesia–thoria–hafnia S. D. Mark jun. (*J. Am. Ceram. Soc.*, 1959, **42**, Apr., 208).

The rational analysis of fireclays W. E. Worrall (*Trans. Brit. Ceram. Soc.*, 1959, **58**, Mar., 145–153, discussion 153–157) Calculation of mineralogical composition is considered. The variation in the composition of 'clay mineral' is shown to be the main source of uncertainty.

IRON AND STEEL, GENERAL

The development of the British iron and steel industry P. Cameron (*J. Jun. Inst. Eng.*, 1959, **69**, Feb., 142–156) A brief review of location, raw materials and processes with some statistics.

Expansion of Spanish steel industry (*Metallurgia*, 1959, **59**, Mar., 139–140) A description is given of the commissioning of three 350-t tilting OH furnaces and ancillary equipment at the ENSIDESA melting shop at Aviles.

Progress at Durgapur (*Iron Steel*, 1959, **32**, Mar., 98–99) Some photographs are reproduced, showing parts of the Durgapur plant being built by ISCON.

Some trends in American metallurgy W. O. Alexander (*J. B'ham Met. Soc.*, 1959, **39**, Mar., 18–33) A review in general terms of government and commercial aspects and research patterns with some account of alloy steels of recent types and of vacuum melting, and non-ferrous topics.

Chile's fully integrated steel expanding and modernizing its facilities (*Blast Furn. Steel Plant*, 1959, **47**, Feb., 172–189) Describes layout, growth and operation of Chile's integrated steel works, the Huachipato plant of the Compania de Acero del Pacifico S.A., which started operation in 1950. Raw material and raw-material sources including coal are fully discussed. The plant has only one blast furnace with a hearth diameter of 20 ft 9 in. The blowers are electrically driven owing to the scarcity of coal.—T.G.

The iron and steel industry of Japan T. Shimamura (*J. Met.*, 1959, **11**, Mar., 163–167) An historical survey. Current problems and achievements of the Japanese steel industry are reviewed.

BLAST-FURNACE PRACTICE AND PRODUCTION OF PIG IRON

Ironmaking: past achievements, present limitations and future prospects J. M. Stapleton, N. W. Lindbloom and D. H. Regelin (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 304–319) An account of the developments in blast furnace design and operation with forecasts of trends in the future.

Modern trends in blast furnace construction H. Steinhaeuser (*Iron Steel Res.*, 1959, **2**, Mar., 939–940) A brief outline of German trends.

Experience in the use of a steep conveyor belt for the transport of coke up to the charging platform H. Hulbek (*Stahl Eisen*, 1959, **79**, Apr. 16, 539–540) The experience obtained with a steep conveyor belt for coke transport to the charging platform of a blast furnace is reported. The belt transports coke at a gradient of 53° over a distance of about 100 ft at a rate of 140 t/hr. The belt has 'humps' at intervals of about 3 ft which prevent the coke from falling back. The belt was, at the beginning, damaged by too small a radius of curvature at the delivery end. In spite of this the service life of the belt will be between two and three years.

The smelting of titaniferous ores H. U. Ross (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 47–64) Smelting of high-Ti ores has been uneconomic because of high fuel consumption and the formation of a pasty slag causing scaffolding in the furnace and clogging of the hearth. A review of the slag system is now presented and also of desulphurization and silicon production. The magnetic concentration and sintering processes should serve to reduce fuel consumption and the use of an acid slag of controlled composition prevents excessive viscosity and accretion formation, but the use of acid slag required desulphurization outside the furnace. There are indications that the use of alkalis might make basic slags usable. The Si content can be controlled by the Ca : Si and Si : Ti ratios and iron with up to 2%Si and not over 0·5%Ti can be produced (19 refs). In discussion, the importance of the slag diagram given is questioned and it is shown that small additions of TiO_2 increase slag fluidity and additions of alkalis often decrease it.

Sintering of high-grade hematite ore E. Klein (*Engineer Foundryman*, 1958, **24**, Dec., 34–38) The effects of charging self-fluxing sinter in ISCOR's No.3 blast furnace are reported. The importance of a low FeO content in the sinter is stressed.—A.G.

Variations in the blast furnace process operating with self-fluxing sinter N. N. Babarykin and F. A. Yushin (*Stal'*, 1958, (12), 1057–1065) The uniformity of reduction of iron oxides in the furnace increases as the burden descends in the stack. The degrees of reduction at different levels are given. Increased reduction in the medium temp. zone greatly improves the furnace coefficient. S level is also measured down the stack, confirming the hypothesis of circulation in the lower part of the column. The plastic state of self-fluxing sinter is reduced in temperature range as the degree of reduction increases. Liquid drop formation is preceded by metal and slag separation within the lump of sinter.—R.S.

Automatic tuyere control cuts blast furnace wear (*Stal*, 1959, **144**, Mar., 9, 78) A brief account is given of an automatic control system designed to balance air flow through blast furnace tuyeres. This is claimed to reduce blowing costs and prevent burning of the furnace lining.—A.G.

Automatic control of blast furnace operations according to static pressure drop N. N. Chernov, P. G. Zhigulev, P. G. Saranovski, V. M. Obsharov, Yu. O. Raev and A. A. Kargin (*Stal'*, 1958, (12), 1071–1077) Control by total pressure drop is not practicable, but control can be based on individual falls of static pressure in the higher zones. A self-recording device for these pressure drops greatly assists control and one suitable for actual operating conditions is described. The effects of pressure drop on working are reviewed.—R.S.

The development of controlled air distribution for the blast furnace J. M. Stapleton (*AISI Yearbook*, 1957, 235–267, discussion 268–270) A review of studies on draught distribution and of the design of an air proportioning system and its operation are given. Hot spots and burned lines are discussed and the advantages of the system on several furnaces and the effects of its going out of operation are described.

Effect of coke size on blast furnace performance D. C. Coleman (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 321–323) The production of uniform coke is considered. **Effect of coke size on blast furnace operation** R. P. Liggett (323–327). **Effect of coke size on blast furnace performance** R. H. White (327–331) An account of a panel discussion.

Blast furnace operation with humidified blast R. J. Wilson (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 4–16) Work on Inland Steel no.5 furnace is described. Steam injection was started in 1957, and comparisons with 1956 are shown, also with furnace no.6 working simultaneously on the same burden. Hearth temperature is controlled more easily and driving rate was increased.

Hot-metal ladle operation in the Magnitogorsk blast furnace shop G. I. Gerasimov, F. A. Korablin, V. M. Nemkin and V. A. Lednov (*Stal'*, 1959, (2), 110–111) Flat-bottomed ladles were found more economical than round-bottomed ladles and flat-bottomed ladles with a circular section conical top should be installed. Wear and methods of relining are discussed.

Process analysis of blast furnace operation with oxygen and steam T. E. Dancy, A. T. Sadler and H. N. Lander (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 16–32, discussion 32–39) A study of operations with 3–6 grains H_2O/ft^3 of blast without O_2 and 5–8 grains at 1·5–2·5% O_2 enrichment is reported. Increases of production and decreases of coke rate resulted. A general method of forecasting improvements to be expected by O_2 enrichment is outlined on the assumption that the thermal requirement per unit of iron is independent of blast compositions.

An investigation of gas flow in a small blast furnace H. W. Hosking, W. O. Philbrook and N. B. Melcher (*AIMME Blast Furn. Comm. Rep.*, 1958, **17**, 138–154) An experimental furnace with a hearth 3 ft in dia., later enlarged to 3 ft 9 in. and a tuyere-to-stockline height of 21 ft 4 in. was used. Gas composition and temperature were measured at three levels, rates and patterns of flow being determined, and also gas transit time using Hg vapour as tracer. Inferences were drawn on localised reaction zones and the mechanism of channeling. The apparatus evolved could be used on a full size furnace.

Results of operations with an experimental blast furnace J. J. Bosley jun. and M. B. Royer (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 256–265) Work with a 3 ft 9 in. dia. furnace with a working height of 21 ft 4 in. is described and the validity of the results is considered.

Wear measurements of blast-furnace brickwork with radioisotopes A. Schepers and F. R. Licht (*Stahl Eisen*, 1959, **79**, May 14, 669–674). Type of blast-furnace brickwork studied and methods of measuring the wear of the brickwork with ^{60}Co are described. It was found that the originally 1000-mm thick brickwork is worn down to about 150 mm immediately above the tuyeres in a few months, in the other parts of the furnace this amount of wear was found after about 12 months.

Desulphurization of hot metal: a production evaluation A. J. Deacon, D. B. Powell and E. C. Rudolphy (*AIMME Blast Furn. Comm. Rep.*, 1958, **17**, 155–168) Work by U.S. Steel on the CaC_2 treatment of steel in the ladle is described. The technique could be applied to routine production. S was reduced from 0·053 to 0·033% by injections of 5·6 lb CaC_2/ton metal with increased production of 2·9–5·5% and decrease of coke rate of 3·75%. Costs are considered.

The desulphurization of pig iron with limestone G. Mahn (*Stahl Eisen*, 1959, **79**, May 28, 815–816) New information on the Kalling and Eketorp technique for the desulphurization of pig iron by limestone treatment is presented. Three conditions for successful operation by this method must be fulfilled: High basicity, low oxygen levels and intimate mixture of the iron with the limestone. Desulphurization to 90–95% is easily obtainable if the operation is carried out in a rotary holding furnace that facilitates intimate mixing. The costs for desulphurization are extremely low.—T.G.

Desulphurization and carburization of molten iron by injection R. Barton and R. B. Coates (*BCIRA Bull.*, 1959, **14**, Mar., 706–716) The uses of CaC_2 and of graphite are reviewed by batch and continuous methods and factors for their successful operation are given.

Bessemerizing the blast-furnace bath K. Kanamori (*J. Met.*, 1959, **11**, Mar., 184–186) In this process, O_2-enriched air is blown directly into the metal bath, thus making this area an actively refining zone, utilising the waste gases produced for pre-heating the charge in the upper part of the blast furnace, and eliminating loss through spattering and excessive oxidation, as compared with convertor refining. Japanese experimental work carried out on 3-t and 1-t/day blast furnaces is described.

Temperature distribution in the blast furnace stove W. A. Knepper and W. W. Campbell (*AIMME Blast Furn. Comm. Proc.*, 1958, **17**, 115–124) Experiments on a 104 ft × 24 ft dia. stove with 82 ft of Kennedy multihole checker brick weighing 1 804 000 lb and with a heating area of 180 581 ft² are described. Gas distribution and temperature distribution over any cross-section were nearly uniform. Heat transfer mechanisms suggest that brick of higher conductivity might be used near the top and a higher gas velocity in the lower part to decrease air-film resistance. A blast temperature of 1 800°F could probably be maintained. Dust accumulation may be as high as 4·5 t/year and 90% of the alkali dust was retained.

Use of automatic stove changing and a new type of stove burner R. T. Hanna and A. J. Karsten (*AIMME Blast Furn. Comm. Rep.*, 1958, **17**, 125–137) Materials beneficiation, humidified blast and furnace enlargement without increase of stove capacity have all called for higher heat output from the stoves. The Fairless automatic changing equipment is described and illustrated and the Freyn-Jansen burner is shown. For a given stove higher blast temp. requires a shorter blast period with either a greater firing rate or more changes per day, and larger burners. Time lost in changing is much reduced by automatic equipment.

50-t capacity railway trucks for the transport of liquid pig iron on public tracks of the German Federal Railway W. Kurscheidt (*Stahl Eisen*, 1959, **79**, Apr. 16, 542–543) The design and construction of a low-loading railway truck incorporating a holding ladle of 50 t capacity are described and illustrated. The truck is intended for use on public tracks in Germany. The ladle is welded, the trunnion plates are riveted to the ladle.—T.G.

The first gas-furnace gas turbine plant in a German integrated iron and steel works K. Muller-Trumbusch (**Stahl Eisen*, 1959, **79**, Apr. 16, 457–463) A 6-mW gas-turbine generator, the first installed in a German integrated iron and steel works, is described. A second set also of 6 mW, is being installed. The turbine uses blast-furnace gas, but heating oil may be used as an alternative at periods when the gas is used for direct heating purposes. The gas is cleaned by an electric filter to dust quantities below 1 mg/m³; heavy deposits in the filter with subsequent reduction in output and efficiency were experienced. The total costs for one 6-mW plant was DM 810/kW, a rather high figure, but production costs of wind and power are very much less as compared with the old plant so that big savings are in fact being made.—T.G.

Electric furnaces to gain in direct reduction of ore (*Steel*, 1959, **144**, Mar. 16, 122–128) After a discussion of the advantages of direct reduction processes as small scale producers of hot metal, the different processes available are briefly reviewed.—A.G.

PROPERTIES, TREATMENT AND USE OF SLAGS

Effect of composition and temperature on the viscosity of synthetic slags E. E. Hofmann (*Stahl Eisen*, 1959, **79**, June 11, 846–854) The ternary system CaO–MgO–SiO_2–Al_2O_3 is discussed and the effect of compositions within this system on the viscosity explained by means of equilibrium diagrams. The effect of other oxides such as BaO, TiO_2, etc., on the slag and its viscosity is briefly mentioned. The additional effect of BaO on the viscosity was studied in detail, the results are presented as graphs. Finally the slags were studied in a hot-stage microscope. The viscometer is described.

Oxide slags, a survey of our present knowledge F. D. Richardson (*Proceedings of the Conference on the Physical Chemistry of Steelmaking sponsored by the Dept. of Metallurgy, Massachusetts Institute of Technology*, 1956, *Dedham, Mass.*, 1958, 55–62) A review of structural and thermodynamic studies. **A summary of some experimental work on the activity of silica in liquid silicate systems** L. Yang, C. L. McCabe, and R. Miller (63–64) A Knudsen cell study is briefly reported. Si vapour pressures and activities were measured. **The activity of silica in lime–alumina–silica slags at 1600°C** F. C. Langenberg, H. Kaplan, and J. Chipman (65–67). **Activities in ternary silicate melts** F. D. Richardson (68–76) The $CaSiO_3$–$FeSiO_3$ and related systems are considered, mainly in terms of free energy (26 refs). **Comments on the equilibrium diagram Al_2O_3–SiO_2** G. Tromel (77–78) The mullite region is particularly considered. **Electrical measurements on oxides and solid cells at temperatures of 1200–1800°C** W. A. Fischer (79–83) The FeO–Al_2O_3 and analogous systems were studied by cells of the type Pt/MeO + Me_2O_3/Me_2O_3/Pt where Me = Fe or Cr. **The action of calcium fluoride in slags** T. Baak (84–86) CaF_2 lowers m.p. and increases fluidity. Possible mechanisms are discussed.

PRODUCTION OF STEEL

Some factors affecting fume evolution from molten steel during oxygen injection K. Knaggs and J. M. Slater (*JISI*, 1959, **193**, Nov., 211–216) [This issue].

Fumes from the acid open-hearth process W. Strauss and M. W. Thring (*JISI*, 1959, **193**, Nov., 216–221) [This issue].

AISI survey on the punched card processing of routinely obtained metallurgical data. (Open hearth furnace practice) (*AISI Contrib. Met. Steel*, 1958, Dec., pp.56) A questionnaire filled in by 48 companies is reported.

Improved method of melting transformer metal N. V. Keis, D. G. Zhukov, and A. M. Khizhnichenko (*Stal'*, 1959, (2), 130–131) A process is described using O_2 and vacuum which greatly increases output and reduces the incidence of surface defects. The equipment used is briefly described and the estimated savings are indicated.

The efficiency of electric steel production by the duplex method of oxygen converter and electric furnace S. D. Gagin (**Kislorod*, 1957, (6), 1–11) Conversion to duplex working is described and many results are tabulated. It is concluded that a cost reduction of 3–7% with a reduced capital investment of 12·2–15·8% could be expected. The supply of scrap was improved, furnace productivity could be doubled and the duplexing method gave more flexible working and better synchronization than OH-electric furnace working. Top blowing with O_2 gave adequate dephosphorization.

Largest arc furnace (*Eng. Found.*, 1959, **24**, Mar., 41) An account of the 30-t furnace installed by Vanderbijl Engineering Corp. for casting steel, the largest in the Southern Hemisphere.

Special shot-blast machine cleans steel ingot moulds P. M. Unterweiser (*Iron Age*, 1959, **183**, Mar. 19, 120–121) Up to 56 moulds per shift can be cleaned using angular shot.

Method of reducing top discard in heavy killed steel ingots M. P. Sabiev (*Stal'*, 1958, (12), 1089–1095) A long boil in the ingot mould leads to reoxidation of metal and saturation of the top third of the ingot with S, P and C and especially with slag oxides causing lamination on rolling. Deoxidation by the C ceases when the mould is capped and the top portion remains saturated with Fe and Mn oxides which have an adverse effect on the welding up of blowholes during rolling. Shortening the period of boil and deoxidizing before capping gives the top of the ingot a semi-killed structure. Best results are obtained with Fe–Si which ensures slow deoxidation.

The mechanism of the influence of magnesium on steel I. A. Andreev and A. Ya. Borisov (*Stal'*, 1959, (2), 131–136) Mg (or Ca) produce deoxidation depending on the time of action and the amounts added. The atomic mobility of dissolved slag is high. The solubility is not greater than some thousandths %. The primary action is on non-metallic inclusions, especially silicates and spinels, in presence of Ti it also reduces sulphide inclusions. Mg is more active than Ni in Si alloys.

FOUNDRY PRACTICE

Cast iron research and development. Where the search for ductility is leading (*Mech. World*, 1959, **139**, Jan., 17–18) Notch ductility, the service of nodular irons at high temperatures, secondary graphitization, heat-treatment of ductile irons, malleable iron at low temperatures and the dry wear of cast irons are briefly reviewed.

Foundries can produce their own cast iron directly from ore H. W. Lownie jun. and A. J. Stone (*Mod. Castings*, 1959, **34**, Jan., 47–54) A number of 'direct-reduction' processes for the production of iron from ore and suitable for small foundries are described and discussed. One feature of all these processes is that the iron is finally produced in an electric furnace. The costs of the process including capital investment are estimated. Some problems involved in direct reduction are specially dealt with.—T.G.

Aid for the design engineer: foundrymen 'accentuate the positive' J. J. Henry (*Mod. Castings*, 1959, **35**, Mar., 53–60) Examples are shown to illustrate the superiority of the cast process over other methods of manufacturing and suggestions are given on how the foundryman can help the design engineer (16 refs.).

Interrelation between stress concentration and castability J. B. Caine (*Mod. Castings*, 1959, **34**, Feb., 101–104) The relationship between shapes designed to minimum stress concentration, maximum load carrying ability and the castability of such shapes is discussed and recommendations for radius design are made.—T.G.

Casting outwears tool steel 25 to 1 (*Prec. Met. Mold.*, 1959, **17**, Apr., 50) Tips for spray gun nozzles of Ni–Cr white cast iron outlasted tool steel.

How to mechanize the smaller foundry H. W. Zimnawoda (*Foundry*, 1959, **87**, Mar., 82–89) Sand handling and preparation can be mechanized with advantage. Step by step modernization is outlined in three stages.

Limited mechanization of the green-sand bay of a batch-production foundry W. Y. Buchanan (*Found. Trade J.*, 1959, **106**, Feb. 5, 143–149; Feb. 12, 175–179) The design, erection, equipment and operation of the new bay is described, and progressive improvement in cupola performance are shown.

Recent developments at Stokes Castings, Ltd (*Found. Trade J.*, 1959, **106**, Feb. 12, 181–182) A brief description of equipment, shot-blasting plant and ventilation.

Modernization creates flexible productivity at Brown & Sharpe L. W. Greenslade (*Mod. Castings*, 1959, **34**, Jan., 32–35) A five-year plan for the modernization of the foundry at Providence, R.I., is discussed.—T.G.

Modernization permits an exclusive product at Tyler Pipe & Foundry J. H. Schaum (*Mod. Castings*, 1959, **34**, Jan., 36–38) The building of the first centrifugal casting plant for 10-ft soil pipe in the United States is described. At the same time, the iron is produced in a basic instead of an acid water-cooled cupola. The pouring operation is described.—T.G.

Modernization yields high productivity at Brillion Iron Works J. H. Schaum (*Mod. Castings*, 1959, **34**, Jan., 39–41) This small foundry at Brillion, Wis., has just put into operation one of the fastest, most mechanized and automated moulding lines in the U.S.A. A brief description of the plant is given.—T.G.

Foundry cuts costs with modern handling methods (*Steel*, 1959, **144**, Mar. 2, 140–142) The benefits following complete modernization of materials handling in a large foundry are described.—A.G.

How science serves Canadian foundries. I S. L. Gertsman (*Can. Metalw.*, 1959, **22**, Mar., 26–32) The author discusses the work of the

Physical Metallurgy Division of the Canadian Dept. of Mines and Technical Surveys. Particular attention is given to research on metal penetration in sand castings, the desulphurization of iron and steel, and the structure of austenitic Mn steel castings.—G.F.

Further developments in Centrispinning high-alloy steels and other materials A. E. Thornton (*Brit. Found.*, 1959, **52,** Feb., 80–90) The physical and metallurgical properties are examined of a range of high-duty components made by the Centrispinning process made from high-Cr, high Cr–Ni, high Cr–low Ni steels and Ni-base alloys. Quality control is discussed.

Shell moulding for steel (*Iron Steel*, 1959, **32,** Feb., 45–48) This is an account, illustrated with photographs, of the production at the Penistone works of David Brown Industries Ltd. of carbon and alloy steel castings by the shell moulding process.

Mechanized shell moulding in an American foundry Evinrude Motors Corp. (*Found. Trade J.*, 1959, **106,** Mar. 26, 352–356) An account of the development and present arrangement of a shell moulding installation for steel crankshaft production. All operations are briefly outlined and illustrations and diagrams are given. A mould is produced every 15 sec and 20 men operate the whole plant.

Resin and sand economy, and surface finish in shell moulds R. S. Jackson and W. W. Glick (*Brit. Found.*, 1959, **52,** Feb., 53–61) Experiments were carried out on shell moulds made from commercial pre-coated sands and from zircon and olivine sands, using various pick-up agents. Cold strength of the test pieces made by blowing into a heated mould were measured using various resin-sand mixtures. It was concluded that substantial savings were possible by sand reclamation and other modifications.

How resins benefit our foundries (*Can. Metalw.*, 1959, **22,** Mar., 22–24) Details are given of the properties and uses of epoxy-resins in the foundry. They combine the advantages of simplicity of manufacture, light weight, high wear resistance, reduced maintenance costs, and long storage life.—G.F.

The practical applications of reinforced epoxy resins for foundry use H. A. Burton (*Brit. Found.*, 1959, **52,** Mar., 127–136) A practical account with descriptions of vacuum bag moulding and vacuum suction methods. Patterns and equipment are illustrated.

Mass effect on castings tensile properties: a rationalization G. W. Form, P. J. Ahearn and J. F. Wallace (*Mod. Castings*, 1959, **34,** Feb., 45–49) An hypothesis is developed for predicting the sensitivity of tensile strength to solidification time. This hypothesis has practical value by permitting the prediction of the tensile behaviour of a cast metal at various section sizes on the basis of limited test data (17 refs.).—T.G.

The effect of mould material on the cooling rate and physical properties of cast metals Sub-committee T.S. 46, Inst. Brit. Foundrymen (*Brit. Found.*, 1959, **52,** Feb., 66–79; *Mod. Castings*, 1959, **34,** Feb., 86–101) This is the second report of the sub-committee. An examination is made of the effects on the physical properties of various cast irons of casting into moulds made in typical moulding sands at various ramming densities, and when using other types of moulding materials. The effect of inoculation and changing the volume/surface area was examined, and a number of practical tests in various foundries was carried out. Other variables being equal it was shown that increased coarseness of grain size and increased mould density increases tensile strength in all cases. The results and their implications are discussed.

Specialized foundry statistical controls improve customer satisfaction B. M. Appleman (*Mod. Castings*, 1959, **34,** Feb., 62–72).

Dust in the ironfoundry H. J. Angus and W. B. Parkes (*BCIRA J. Res. Dev.*, 1959, **7,** Feb., 624–636) The major sources of dust in the iron foundry are stated and dust composition and methods of estimation are described. Control methods for preventing pneumoconiosis are recommended for four main types of foundry.—A.G.

VACUUM METALLURGY

Melting in a vacuum: alloy steels with minimum contamination (*Engineering*, 1959, **187,** May 8, 626–627) An account of the two furnaces now in operation at Brightside.

Effect of melting practice upon the properties of stainless steel R. S. Belyakov (*Primenenie Vakuuma v Metallurgii*, Moscow, 1958, 35–48) Cr–Ni stainless free from susceptibility to intergranular corrosion can be produced by vacuum melting. By holding for 30–40 min carbon can be reduced to 0·02%. This and the low Ti content gives high resistance to boiling 50% HNO_3.

Vacuum pouring for large forging production H. C. Bigge and E. A. Reid (*Iron Steel Eng.*, 1959, **36,** Apr., 129–141) The development is described of the vacuum casting plant at Bethlehem Steel Corporation. Ingots up to 120-in. dia. can be treated, and a T.V. camera is used for inspection.

Experience of the large-scale vacuum-treatment of open-hearth steels C. H. Pottgiesser, H. J. Darmann and A. Drevermann (*Stahl Eisen*, 1959, **79,** Apr. 16, 463–468) The plant operating by the syphon principle is described. The time from end of tapping to beginning of teeming is 10 min for untreated charges and 27 min for vacuum-treated charges. The vacuum-treatment and the results obtained with it on OH steels are described in detail and presented as graphs. The superiority of vacuum-treated steel is obvious, this is shown in an increase of 5% in the yield of the production. It seems that cavities which were detected in ingots by ultrasonic methods weld up properly during hot rolling and do not lead to lamination.—T.G.

Observations made on the properties of vacuum-treated steels C. H. Pottgiesser, A. Wicher and E. Klein (*Stahl Eisen*, 1959, **79,** Apr. 16, 468–472) Examination of steels vacuum-treated by the syphon method showed that the quantity of non-metallic inclusions is low and the inclusions are finely dispersed throughout the ingots. Steels killed with Al showed great uniformity in the amounts of dissolved Al. The same uniformity as in the composition was found in the physical and mechanical test values, there is no difference in the data for test pieces taken in the longitudinal and transverse directions. The steel consumers report better working properties of the vacuum-treated steels.—T.G.

Experience gained in the degassing of steel by the vacuum-teeming method A. Tix, G. Bandel, W. Coupette, and A. Siekbert (*Stahl Eisen*, 1959, **79,** Apr. 16, 472–477) The vacuum-degassing of steel during teeming (or alternatively during teeming into a holding ladle) is described and the superiority of the vacuum-treated steel with respect to chemical composition, mechanical strength, freedom from non-metallic inclusions and internal cracks is stressed.—T.G.

On the various properties of vacuum-melted heat-resistant Nimonic 80A, S816 alloys and 13% chromium stainless steel S. Koshiba and T. Kuno (*Nippon Kinzoku*, 1958, **22,** Apr., 169–173) With vacuum melting, as compared with normal melting, 13% Cr stainless steel had lower contents of gas and non-metallic inclusions, and the mechanical properties at high temp. and corrosion resistance were improved. Similar improvements, including improved workability and rupture strength, were found with Nimonic 80A and S816.

REHEATING FURNACES AND SOAKING PITS

Evaluation of soaking pit requirements for an expansion program G. J. Meng (*Iron Steel Eng.*, 1959, **36,** Mar., 117–122) An account is given of an investigation with the object of developing more efficient operation of a soaking pit plant with a view to expansion of output.

Recent furnace installations O. D. Rice and R. W. Ewalt (*Iron Steel Eng.*, 1959, **36,** Apr., 102–110) Modern developments in the design of bloom and slab reheating mills are discussed. Diagrams and photographs of typical installations are reproduced, and examples are given of the reduction in down time due to replacement by erecting the new furnace on rollers near the existing furnace, and increasing strip mill reheat facilities without using additional floor space.

The characteristics of a high-load continuous furnace S. Muramatsu (*Tetsu to Hagane*, 1958, **44,** Sept., 992–994) Relationships are shown between heating rate and slab thickness; heating rate, cover ratio and waste gas temp.; and air flow and heat-transfer coeff.—K.E.J.

Calculation of reheating processes of steel ingots as compared with actual measurements R. Wartmann (*Stahl Eisen*, 1959, **79,** May 14, 703–711) The problem of reheating large steel ingots without inducing too large temperature differences between surface and core is treated mathematically. The results of the numerical calculations carried out on computers are in good agreement with experimental results.

Heating for forging in a batch-type furnace P. C. Finlayson and J. S. Schofield (*JISI*, 1959, **193,** Nov., 238–252) [This issue].

Statistical evaluation of ingot heat utilization and of the heating time in soaking pits Z. Volf and E. Gladyš (*Hutn. Listy*, 1959, **14,** (2), 98–104) [In Czech] Heating time of ingots of various types of steel were determined in soaking pits. The ingots were contained in moulds or were stripped. Heating time was also determined for various surface temperatures of the ingots at charging time and for various starting temperatures of the furnace. The measurements were evaluated for establishing the optimum time between teeming of the ingot and charging into the soaking pit with respect to best utilization of the ingot heat (22 refs.).

HEAT-TREATMENT AND HEAT-TREATMENT FURNACES

Modern heat-treatment equipment L. G. W. Palethorpe (*Wild-Barfield J.*, 1959, **7,** Mar., 2–8) A review of batch-type controlled atmosphere and continuous controlled atmosphere furnaces.

Flexible heat treat line keeps jobbing economical (*Steel*, 1959, **144,** Mar. 23, 122–124) A description is given of a new heat treatment installation for precision gears, comprising gas generators, a spray washer, tempering furnace, pot furnace and two controlled atmosphere pit-type furnaces.—A.G.

Economic advantages of gas-fired heat treating furnaces F. C. Schaefer (*Met. Treating*, 1959, **10,** Jan.-Feb., 2–3, 35, 48–50) A companion article to one on electric furnaces. A survey with some cost comparisons.

Developments in the use of town gas in industry – 2 S. G. Aberdein and A. E. Tyrrel (*Ind. Gas*, 1959, **22,** Jan., 18–21) Uses in the iron and steel industry, for annealing, hardening and similar uses with or without protective atmospheres are briefly referred to.

Gas heat-treatment furnaces for metals L. Walter (*Gas World*, 1959, **149,** Jan. 17, 11–13) A review of general principles with recommendations for maintenance and operation.

Generating protective atmospheres (*Prec. Met. Mold.*, 1959, **17,** Jan., 44) A brief account is given of a protective atmosphere generator designed by Lindberg Engineering Co., using a synthetic zeolite which selectively absorbs CO_2 and water vapour. A flow diagram is given.

Salt bath heat treatment equipment and methods A. G. Gardner (*Mech. World*, 1959, **139,** Mar. 117–123) The author describes the basic characteristics of salt baths for heat treatment, and compares these characteristics with those of conventional furnaces. The different types of salt bath and their operation are discussed, and some typical applications are considered. These include annealing, neutral hardening, hardening of high-speed steels, carburizing, cyaniding, and interrupted quenching.—G.F.

How to increase high temperature reliability of A-286 fasteners (*Steel*, 1959, **154,** Mar. 16, 114–117) Various design and fabrication techniques are suggested for increasing the life of A-286 bolts. The recommendations are based on a study of the effects of heat treating temperature, method of forming, surface finish and thread contour on bolt performance.—A.G.

Metallurgical research develops improved products through heat treating H. Hoover (*Met. Treating*, 1959, **10**, Mar.–Apr., 8–13, 40, 42–45) A full account of the development of discs for harvesters. A complete set of tests on C–1085 strip steel was carried out and new tests were devised and heat-treatments were explored. A full set of structures is illustrated.

Wall construction of hardening furnaces C. L. Beuken, J. de Boer and L. Smeets (*Elektrowärme*, 1959, **17**, May–June, 198–206) Work at Maastricht on a model for the best construction for intermittent operation, continuous day operation, continuous operation with light or heavy walls is reported. Output, energy consumption, temperature variations and losses by radiation are assessed.

One set-up hardens and grinds slideways T. S. Harrison and Sons Ltd. (*Metalw. Prod.*, 1959, **103**, Mar. 6, 406–408) A combined induction hardening installation and surface grinder for lathe beds is described.

What controls are needed for accelerated carbonizing P. M. Unterweiser (*Iron Age*, 1959, **183**, Jan. 15, 86–88) A laboratory study of carbonizing in a graphite pot is described and various aspects of carburizing and the properties of the case and of the structures produced and heat treatments are discussed.

The prediction of carburizing cycles E. N. Case (*Met. Treating*, 1959, **10**, Mar.–Apr., 2–7, 39) Time taken to attain carburizing temperature is considered. Curves are presented and case depth, carbon potential and carbon solubility are discussed. The use of carburizing equations is exemplified.

High temperature carburizing H. N. Ipsen (*Met. Treating*, 1959, **10**, Jan.–Feb., 4–8, 42) Tests on a 0·2%C, a Ni–Mo and two Ni–Cr–Mo steels were carried out at 1700°, 1800°, 1900° and 2000°F, with special attention to grain growth effects. The results were satisfactory especially with reheat hardening. Structures are shown and costs compared.

Automatic flame-hardening techniques: heat treat giant excavator components (*Metalw. Prod.*, 1959, **103**, Mar. 20, 491–493) An account of operations at Ruston-Bucyrus Ltd., Lincoln.

Special oxy-town gas equipment for flame-hardening work (*Ind. Gas.*, 1959, **22**, Apr., 7–10) An account of the Ruston-Bucyrus installation at Lincoln. **Flame hardening by the oxygen-town gas process** (*Heating*, 1959, **21**, Apr., 126–127).

Flame-hardening: use of oxy-town gas (*Met. Treatment*, 1959, **26**, Apr., 169–170) An account of the Ruston-Bucyrus installation at Lincoln.

Flame-hardening with oxy-town gas (*Engineer*, 1959, **207**, Mar. 20, 449–450) An account of the installation for hardening excavator parts at Ruston-Bucyrus Ltd., Lincoln.

Flame-hardening operations on components for earth-moving equipment (*Machinery*, 1959, **94**, Apr. 8, 800–801) Peddinghaus equipment for hardening by the spinning method and for roller path and internal gear hardening are illustrated.

Heat treatment of steel strip J. McMullen (*Sheet Metal Ind.*, 1959, **36**, Jan., 5–25) Furnace atmospheres are reviewed for annealing and for carbonitriding and heat transfer is briefly considered. Quenching hardening and tempering and the various types of furnace are discussed. Further points are brought out in discussion.

How to carbonitride low carbon steel (*Steel*, 1959, **144**, Feb. 23, 86–88) The optimum conditions have been determined and are reported for carbonitriding 0·2%C steel in a shaker hearth furnace.—A.G.

Bright annealing A. E. Pickles (*Met. Treatment*, 1959, **26**, Jan., 7–14– A review, showing the type of atmosphere required for mild, carbon and stainless steels and non-ferrous metals and the type of furnace required for various operations. These furnaces are briefly described and illustrated with notes on their operation.

The use of producer gas as protective atmosphere in the annealing of thin steel sheet J. Můčka and J. Teindl (*Hutn. Listy*, 1959, **14**, (2), 104–110) [In Czech] In the bright annealing of thin, low-carbon steel sheet with producer gas the sulphur content of the gas proved to be annoying. The authors developed filters for the absorption of the sulphur from the producer gas and showed that this purified gas has the same good protective properties as other gases which are much more expensive to produce (12 refs.).—T.G.

Some aspects of controlled annealing atmospheres for cold rolled strip in coil J. Higo (*Toyo Kohan*, 1955, **4**, Dec., 49–60) A review of various types of protective atmosphere used for annealing cold-rolled strip in coils, particularly that destined for tin-plating.—K.E.J.

On-site heat treatment (*Overseas Eng.*, 1959, **32**, June, 368–369) Methods used for the on-site annealing of welded constructions by electrical heating are briefly considered.

Electrical equipment for a high-speed annealing line J. E. Butler and W. G. Cook (*Iron Steel Eng.*, 1959, **36**, Mar., 89–96) The electrical equipment is described for the Weirton high-speed continuous strip annealing line started up in 1957. It is designed to handle 45-in. wide, 85-in. dia., 60,000-lb. coils at speeds up to 2000 ft/min.

Continuous annealing 60 tons per hour of tin plate Q. M. Bloom (*Iron Steel Eng.*, 1959, **36**, Feb., 115–125) An account of the operation of the Sparrow Point continuous annealing plant, the advantages of a multi-line plant of moderate speed being emphasised.

Post-weld annealing of stainless steel M. W. Wilson (*Mech. World*, 1959, **139**, Jan., 30–31; *Corros. Prev.*, 1959, **6**, Jan., 62) A brief account of the difficulties (high temperature, rapid heating and protection from scaling) in the solution of precipitated carbides and the development of steels not requiring this treatment is given.

Compact furnace anneals strip D. L. Somers and C. A. Turner (*Iron Age*, 1959, **183**, Feb. 26, 94–95) A new continuous annealing line used in conjunction with a Sendzimir mill for stainless strip production is briefly described.

Heat-treatment. Method of preheating and stress-relieving welds Electrothermal Ltd. (*Aircraft Prodn.*, 1959, **21**, Feb., 82) Movable armoured resistance heaters are shown.

Heat treatment of high duty stampings (*Mech. World*, 1959, **139**, Jan., 21–22) An account of the new Darley Dale plant.

Remote control in tube manufacture (*Iron Steel*, 1959, **32**, Mar., 119–120) A description is given of the new electronically controlled system in the gas-fired, roller hearth annealing furnace installed by Birlec Ltd. in the new stainless steel mill of Talbot Stead Ltd. at Walsall.

Development of procedure for casting and heat treating vertically cast cored steel plate J. W. A. Tyler (*WAL RPL* 11–2; *PB* 133277; 1957, July, pp. 12) Procedures are described. The properties required were obtained by high-temperature homogenizing, austenitizing, quenching and tempering.

Modern quenching oils and techniques for foundry heat-treating N. F. Squire (*Mod. Castings*, 1959, **34**, Jan., 76–80) The importance of heat-treating steels, in particular the quenching phase, is stressed and the conventional quench, marquench and modified marquench are dealt with. The benefits obtainable from using an additive-type quenching oil are shown in a number of case histories.

Quench and temper process for the manufacture of high strength tubular products E. B. Emerick (*AISI Yearbook* 1957, 107–138, discussion 139–152) An account of the needs and expansion of the petroleum industry and its demands for pipe. Deep drilling and the properties of pipe necessary, quenching and tempering with a description of the Jones and Laughlin line, and mechanical properties and performance are reviewed. In the discussion A. B. Wilder gives an account of the production of N80 casing.

Comparison of deterioration resistance of marquenching oils M. Okamoto and R. Tanaka (*Nippon Kinzoku*, 1958, **22**, Apr., 173–176) Three oils are compared for resistance to deterioration by assessing their physical properties and cooling ability when fresh and after a period of use.—K.E.J.

Developments in the tempering of steel (*Mech. World*, 1959, **139**, Mar., 115–116) Some of the more recent discoveries and advances in the knowledge of the principles and effects of the tempering process are summarized. The points discussed include the effects of composition, time, and temperature; the phase transformations involved; and the phenomena of quench tempering and temper embrittlement.

FORGING, STAMPING, DRAWING AND PRESSING

The forging industry: past, present and future G. H. Jackson (*Met. Treatment*, 1959, **26**, Apr., 137–144; May, 191–197) The current lecture course at the Wolverhampton and Staffs. College of Technology. A mainly historical account of the development of equipment is given with extension to press forging, reducer rolls, upsetting and non-ferrous applications. A brief section on defects in steel and notes on spark erosion, cold extrusion, furnace design and future requirements are added.

Experimental semi-automatic forge J. I. T. Green and J. D. Stringer (*JISI*, 1959, **193**, Nov., 231–237) [This issue].

Automated forge shop saves GM 25% (*Steel*, 1959, **144**, Jan. 19, 76–77) The automatic hot forging of yokes for car transmission is reported to have led to large cost savings. Conveyors are coordinated with micro-switches to give an even flow of material. Use of a graphite-based lubricant is essential for smooth operation.—A.G.

Forging large crankshafts (*Machinery*, 1959, **94**, Jan. 7, 33–36) An illustrated account of the large drop hammer installed at Park Drop Forge Co., Cleveland, Ohio, rated at 50000 lb forging crankshafts up to 12 ft 4 in long. Three presses are also provided.

Heat reflectors in forging A. R. Hardy and J. D. Stringer (*JISI*, 1959, **193**, Nov., 227–231) [This issue].

Forgeability of steels: a critical survey of the literature G. P. Contractor and W. A. Morgan (*Met. Treatment*, 1959, **26**, Feb., 65–71) A review (43 refs.).

How to evaluate formability of sheet metal D. A. Stewart (*Mat. Design Eng.*, 1958, **48**, Dec., 100–102) Bend tests and draw tests for sheet metal and the machines to perform the tests as well as interpretation of the test results are discussed.—T.G.

Fabricating techniques for high strength superalloys H. E. Haley (*Western Metalw.*, 1959, **17**, Mar., 52–55) Steels, Fe–Ci–Ni–Co and other alloys are considered. Forming heat-treatment, forging and welding properties are given.

Vibration-controlled hammer: Installation of foundation at Smith's Stamping Works R. Hammond (*Met. Treatment*, 1959, **26**, Feb., 73–76) An illustrated account.

Vibration-controlled foundations at Saltley R. Hammond (*Iron Steel*, 1959, **32**, Mar., 107–111) An account is given of the measures taken to control vibration in the reconstructed foundations at the forge of the Metropolitan Cammell Carriage and Wagon Co. at Saltley, Birmingham.

Metal flow in forging. X: Design of a ball rolling die. II. T. Awano and M. Ochiai (*Rep. Govt. Ind. Res. Inst. Nagoya*, 1959, **8**, Feb., 91–98) The design of multiple thread dies is demonstrated and a profile change of screw thread along the axis at the inlet part of the rolls and the roll form for preventing defects at the cut-off of rolled balls are considered. The use of the multiple thread does not appear to be advantageous for facilitating feed of work material. A screw height change for the inlet is recommended. Cross-section, power and the prevention of the defect of the cut-off are the subject of recommendations. **XI. Experimental ball rolling machine** T. Awano, M. Ochiai, S. Ogiso, K. Hasegawa, and K. Higuchi (Mar., 151–159) An experimental hot ball rolling machine is described using two kinds of rolling dies. Guide plate location, rod diameter and length, number of revolutions and power required and the effect of temp., dimensional accuracy and metal flow were studied.

On special features of calculating the stress condition appearing in forging flat blocks A. D. Tomlenov (*Vestnik Mashin.*, 1959, (3), 46–47) Tensile stresses may result in axial or transverse cracks in forgings, more especially in the

case of metals of low plasticity or in cases of low forging temperatures. It is therefore advisable not to limit the upper forging temperature as is recommended in the literature, because although a low final forging temperature favours a fine-grained structure, it also causes a significant reduction in plastic properties and causes longitudinal and transverse cracks. The use of an insufficiently powerful plant and of too narrow hammers may also result in the formation of transverse cracks.

Improving the wear resistance of dies by treatment with colloidal graphite F. Neuberger (**Fertigungstechnik*, 1956, **6,** July, 321–323) Forging trials are reported, the method of lubrication being carefully standardized and the rise of the temperature of the dies was noted as well as the wear incurred. At least 16% longer life was attained in all trials though the difficulties of generalizing the results are stated.

Covmac all-steel automatic horizontal forging machine Coventry Machine Tool Works Ltd (*Machinery*, 1959, **94,** May 6, 1035–1036) A machine with an output of 4800 parts per h $1\frac{1}{4}$ dia. × 5 in. long is shown.

Erase draft while forging to save machining steps H. Chase (*Iron Age*, 1959, **183,** Feb. 26, 98–100) By eliminating draft in drop forging, subsequent machining may be reduced. Means of achieving this in a number of forging applications are discussed.—A.G.

Problems regarding concentration and co-operation of press and forge production in the Urals Z. S. Drugaleva (*Vestnik Mashin.*, 1959, (2), 72–74) Because of departmental separation of plants in the Urals a large number of small units have been put in production which exist alongside some very large undertakings. The article discusses the economic and production problems raised by this state of affairs.

Drives of forging presses F. Riemenschneider and K. Nickrawietz (**Stahl Eisen*, 1959, **79,** Apr. 16, 494–500) The differences in the various types of hydraulic forging presses, their advantages and useful applications are explained.—T.G.

The cold extrusion of steel R. A. P. Morgan (*JISI*, 1959, **193,** Nov., 285–303) [This issue].

The extrusion of high-speed steel sections P. J. Sukolski and G. Hoyle (*JISI*, 1959, **193,** Nov., 270–277) [This issue].

The extrusion of steel tubes J. W. R. Naden (*JISI*, 1959, **193,** Nov., 278–284) [This issue].

ROLLING MILL PRACTICE

A method of sampling inspection by variables on the weight of cold rolled sheets Y. Yoshimura, T. Torihata and S. Asada (*Toyo Kohan*, 1953, Dec., 52–55) A statistical scheme is set out for weight inspection of cold-rolled sheets which are shorn on indicated size and sorted to indicated pounds.—K.E.J.

An optical technique for continuous measurement of the width of narrow hot rolled strip C. Burns and J. W. Stevens (*Instr. Practice*, 1959, **13,** Mar., 277–282) An installation with an accuracy of ±0·005 in. with a response speed of 0·1 sec. independent of transverse shake in the strip is described. Air purging avoids interference by scale dust.

Reeling and coil conveying equipment in wire rod and merchant mills A. Hinders (*Wire*, 1959, Feb., 3–10) The design and operation of reelers and conveying equipment for wire coils and merchant bar are described, with illustrations of the equipment.

A constant horsepower computer – permitting the use of slip couplings for winder drives P. R. Gravenstreter (*Iron Steel Eng.*, 1959, **36,** Jan., 79–83) It is shown that regulated constant H.P. required to coil strip at uniform strip tension can be obtained from an a.c. induction motor and an electric slip coupling, the excitation of which is controlled by a constant H.P. regulator.

MACHINERY AND SERVICES FOR IRON AND STEEL PLANT

The design of crane-hook screw-joints for even stressing L. Maduschka (**Stahl Eisen*, 1959, **79,** May 28, 797–802) Mathematical treatment of the problem to provide even loading of the threads of the joint, mainly by eliminating stress-raisers.—T.G.

Rationalization of the road transport in integrated iron and steel works H. J. Stengert (*Stahl Eisen*, 1959, **79,** Apr. 16, 522–533) Organization, maintenance and costs of internal road transport in integrated iron and steel works are reported and discussed.—T.G.

ERRATUM

JISI 1959, **191,** Apr., p.404c, line 19.
'Casting cranes of heaviest construction'
The reference should be *Demag Nachrichten*, 1957, (150), 16–20, and the title
'High-capacity ladle cranes'.

WELDING AND FLAME-CUTTING

Welding in marine engineering J. A. Dorrat (*Trans. Inst., Marine Eng.*, 1959, **71,** Mar., 69–88, discussion 89–95) Properties of suitable steels, methods of welding properties of welds and good design are discussed. Inspection and testing are included.

How to do a better welding job R. Kolb (*Eng. Min. J.*, 1959, **160,** Jan., 71–78) A table of tests for the identification of the common metals is given, including white and grey and malleable irons, wrought iron, low and high-carbon and alloy steels. Choice of process for the welding of the identified components is then discussed. Welding electrodes are separately described and tables of filler metals for stainless steel and of electrodes for cast iron are added.

Some aspects of the metallurgy of welding and fabrication of steel H. C. Skevington (*Sheet Met. Ind.*, 1959, **36,** Feb., 85–90) The localized melting and consequent alteration of the adjacent zones and their effects are described and sections are shown. The effects of steel composition and the carbon equivalent formula are discussed. Flame cutting and hot and cold working are also briefly considered.

The value of nitrogen as weld backing gas R. V. Anderson (*Weld. J.*, 1958, **37,** Dec., 1193–1195) As high-nickel and martensitic stainless steels are relatively insensitive to N_2 backing-gas pick-up, it may be substituted for A with little or no detrimental effect at a cost saving of approximately 90%. The study reported here indicates that N_2 may be safely introduced into the weld metal at a maximum level of 0·01% for each 1%Cr in the base metal.—T.G.

Carbon-dioxide-shielded metal-arc welding of carbon-steel plate R. J. Keller (*Weld. J.*, 1959, **38,** Jan., 27s–38s) The advantages and limitations of the CO_2-shielded metal-arc welding process for carbon steel plate up to thicknesses of 1·5 in. with up to 0·30%C are discussed and recommendations made for successful operation, for instance, on suitable filler wire, in design of joints, etc. Safety requirements are also dealt with.—T.G.

Inert-gas consumable-electrode welding of thin material T. McElrath (*Weld. J.*, 1959, **38,** Jan., 28–33) An illustrated report on the advances made in the welding of thin materials by the inert-gas consumable-electrode process. This has been achieved by a combination of the short-arc technique, suitable constant-potential power supply, and the ability to feed small-diameter wire. Stainless steel and carbon steel down to 0·015 in. thickness were successfully welded.—T.G.

Welding stainless steel to carbon or low-alloy steel J. J. B. Rutherford (*Weld. J.*, 1959, **38,** Jan., 19s–26s) The author traces the historical record of development and successful application of the practice of welding stainless steels to ordinary steels, considers difficulties which have arisen, and proposes aids and limitations (18 refs.).—T.G.

Joining of Zircaloy to stainless steel J. B. McAndrew, R. Necheles, and H. Schwartzbart (*Weld. J.*, 1958, **37,** Dec., 529s–534s) Flash welding and brazing of Zn to stainless steel heavy-wall tubing was studied for the production of a metallurgical seal that would withstand long exposure to pressurized high-temperature water. Although some weldments showed promise of meeting the requirements, reproducibility was poor in both cases because of thermal stresses.—T.G.

Effect of zinc coatings on resistance seam welding of sheet steel C. W. Volek and M. L. Begeman (*Weld. J.*, 1959, **38,** Mar., 113s–121s) A study of the effect of the Zn coating is reported and the major variables of the process are identified and evaluated and represented as curves and tables with illustrations of structure. Conclusions are set out and a satisfactory welding procedure is arrived at.

How to weld copper to stainless steel and mild steel H. B. Bott (*Weld. J.*, 1959, **38,** Mar., 236–238) Practical details are given with a series of illustrations.

Welding and forming: Development of special equipment for aircraft alloys Morfax Ltd (*Aircraft Prodn.*, 1959, **21,** Mar., 98–105) Argon-arc precision welding of stainless steel is included.

Electroslag welding comes to UK (*Metalw. Prod.*, 1959, **103,** May 29, 950–954) The Russian process using an electrically conducting pool of slag has been introduced into this country from Vus of Czechoslovakia. The installation is described and illustrated. The Unionmelt machine is also described.

Short arc pinpoints heat for welding sheet metal (*Iron Age*, 1959, **183,** Feb. 26, 90–92) A stable buzzing-type arc is described which makes possible the rapid welding of light gauge sheet metal.—A.G.

Why arc welding cast iron is a problem H. Thomasson (*Can. Metalw.*, 1959, **22,** Mar., 36–38) The author describes the different types of micro-structure which can be found in cast iron, and explains why the structure makes cast iron difficult to weld. The necessary care and precautions in the welding procedure are outlined.—G.F.

Success-welding method for type 347 A. M. Bain, A. H. Clark, and M. J. Lavigne (*Can. Metalw.*, 1959, **22,** Feb., 28–34, 38) The authors describe what is believed to be the first successful welding technique for type 347 stainless steel. The technique was developed in fabricating a test loop for Atomic Energy of Canada Ltd, and some of the unusual problems faced in the design and fabrication are discussed.—G.F.

Fabricating high-strength superalloys H. E. Haley (*Met. Ind.*, 1959, **94,** Mar. 13, 203–206) Superalloys, mainly containing Fe, are classified and their welding is discussed. Rupture strengths at 650°, 820° and 980°C are given for 100 and 1000 h periods and elongation values are shown.

The metal-arc welding of stainless steels M. C. T. Bystram (*Welder*, 1959, **28,** Jan.–Mar., 17–22) Structures are shown and their dependence on composition is discussed. Various defects are noted and recommendations for producing joints of high radiographic quality are made.

Heavy plate cutting B. Brasseur (*Weld. Met. Fabr.*, 1958, **26,** Dec., 443–448) The process of oxygen cutting is described with special reference to faults and their prevention.

MACHINING AND MACHINABILITY

Machining of high-alloy steels on chromium-, chromium–nickel- and chromium–nickel–manganese bases G. Menges and H. Treppschuh (*VDIZ*, 1959, **101,** Mar. 1, 265–273) The relationship between machinability and microstructure of these high-alloy steels is discussed and recommendations are made for the various machining operations. The importance of proper cleaning of all machined parts of these steels is stressed.—T.G.

Study on leaded free-cutting steels VIII. Study on performance and utility of leaded free-cutting steels for automobile parts M. Osawa (*Tetsu to Hagane*, 1958, **44,** Sept., 1099–1101) Results of fatigue, bending and service tests are given for steels of lead content varying from 0 to 0·31%.—E.E.J.

Motion-picture technique for studying grinding phenomena K. E. Schwartz (*Eng. Dig.*, 1959, **20,** Mar., 115–117, from *Industrie-Anzeiger*, 1958, **80,** Nov. 18, 1391–1395) Stages

of wear in grinding wheels are illustrated and discussed.

X-ray studies on metals after electro-spark working K. F. Starodubov and B. P. Kolesnik (*Fiz. Met. Metallov.*, 1957, **5**, (3), 434–441) Data are given of the phase structure and microhardness of a large number of metal combinations after electro-spark working. Fe (as cathode) was worked with Mn, Fe, C, Al, V, Ni, Cu, Zr, and Nb electrodes (anodes). Tables showing the results of X-ray examination of the surface layer, and of micro-hardness tests are reproduced.

CLEANING AND PICKLING

How to choose the right cleaning process D. J. Fishlock (*Metalw. Prod.*, 1959, **103**, Feb. 6, 223–228; Feb. 13, 277–282) A review of methods and installations for the removal of greases, oils, lubricants, swarf, sand, polishing residues, and casual dirt. In Part II, the use of emulsion, solvent, and ultrasonic methods are considered with some specialized techniques for stubborn deposits, such as sodium hydride treatment.

Preparatory treatments for protective coatings I. Derusting and descaling (*Corros. Prev.*, 1959, **6**, Feb., 41–43) A brief review of methods in use.

Mechanical blast-cleaning of carbon and alloy steel strip G. D. Dill (*Iron Steel Eng.*, 1959, **36**, Feb., 149–153) The efficiency and economic operation of mechanical blast cleaning of steel strip are shown by examples.

Shot-blasting proves practical in high-speed rod line (*Iron Age*, 1958, **182**, Dec. 18, 89–91) Acid cleaning has been successfully replaced by shot blasting in a continuous wire and rod mill. Advantages claimed include lower metal loss and no waste acid disposal problem.—A. G.

Development and practical application of shot blasting in the shipyard (*Corros. Prev.*, 1958, **5**, Dec., 40–43) Shot blasting, with round, specially-treated cast steel shot of a grade equivalent to B.S.S. 30 mesh, of ship's plates and sections and bulb angles is described. The plates receive a protective coating.—T. G.

The development and practical application of shot blasting in the shipyard (*Bradley's Mag.*, 1959, Spring, 3–4, 6) Installations built by Tilghman's Ltd, Altrincham, Cheshire, are described.

Mechanical descaling for wire drawing: A survey of the literature G. McH. Sturgeon (*Iron Steel*, 1958, **31**, Dec., 563–568) Mechanical descaling is rather a new process, introduced during the wars when production was increased and acid short. The author illustrates and reviews the various processes employed and their effectiveness; shot-blasting giving the best results. Problems in the use of mechanical descaling and economic considerations are dealt with. It is expected that tightening up the regulations on water pollution will promote the application of mechanical descaling on a much wider scale than hitherto (26 references).—T. G.

Oxidation of iron pretreated for porcelain enamelling L. E. Fussell and R. L. Hadley (*Inst. Vit. Enamel. Bull.*, 1959, **9**, Jan., 72–80) Sulphuric acid pickle, nickel flash and phosphate treatments on iron were compared for thickness of oxide layer formed at different temperatures by X-ray and metallographic methods. Ni-flash produced lower rates and phosphating produced significantly rougher interfaces. Armco iron was used $4 \times 12 \times 0{\cdot}036$ in.

Hydraulic jet descaling – a new method to remove scale from hot steel B. G. Harmon and R. V. Proctor (*Iron Steel Eng.*, 1958, **35**, Dec., 97–108) The design is described and results discussed of a hydraulic jet descaler for descaling billets, slabs, and continuous strip or plate mills.

Electrochemical studies on pickling I. Potential–time curve measurement for the determination of pickling rate I. Yano (*Tetsu to Hagane*, 1958, **44**, Sept., 997–998) Potential–time curves are given for varying H_2SO_4 concentrations and different specimens of 0·05%C, 0·15%Cu steel. The effects of varying H_2SO_4 and $FeSO_4$ concentrations at 80°C, and H_2SO_4 concentration and temp. for 10% concentration of $FeSO_4$, are studied.—K. E. J.

Effect of halide addition to the sulphuric acid pickling bath M. Haftel and W. L. Reichel (*PB* 131794, 1957, Feb., pp.21) Addition of 0·1–1% sodium chloride or iodide to inhibited baths for mild and special treatment steels was examined. It has no effect on scale removal rate but slightly increases the effectiveness of the inhibitor the effect increasing with time of treatment. Addition of 1% NaCl to the bath is recommended.

Controlled vibrations speed and simplify parts finishing W. E. Brandt (*Iron Age*, 1959, **183**, Jan. 15, 79–82) A new method of tumble finishing metal parts is described, based on the controlled 3-way vibration of a simple container loaded with work-pieces and finishing media.—A. G.

Treatment of effluents in the metal finishing industry R. Justh (*Electropl. Met. Fin.*, 1959, **12**, Jan., 11–14; Feb., 47–50) Acid, alkaline, pickling, bright dipping and plating and cyanide effluents are briefly reviewed. Hypochlorite treatment of plating solutions containing cyanide and treatments of chromate-containing effluents are outlined.

PROTECTIVE COATINGS

Mechanism of electrolytic deposition and dissolution of metals H. Gerischer (*Anal. Chem.*, 1959, **31**, Jan., 33–39) A review of recent theory and of experimental methods, and of the mechanisms of complex ion discharge.

Technical developments of 1958 N. Hall (*Met. Fin.*, 1959, **57**, Jan., 38–47, 107–108) A review of the finishing trade and patent literature relating to technical developments in preparation, coating, testing, and control (417 refs).

Electrochemistry and the plater F. G. Brune (*Products Fin.*, 1957, **21**, Aug., 44–48; Nov., 68–70, 72, 74) The electrochemical series, sign conventions, and the Periodic System are outlined. Ions and their numbers, movement and velocity and transport members are then considered in non-mathematical terms.

Electrochemistry and the plater III. F. G. Brune (*Products Fin.*, 1958, **22**, Mar., 36–38, 40, 42) The theories of solution pressure and standard electrode potentials are developed.

Feedback theory of hydrogen overvoltage J. F. Chittum (*Nature*, 1959, **183**, Feb. 28, 589–590) A mechanism based on surface complex formation is described.

Adsorption phenomena and cathodic processes in the electrodeposition of iron in the compact and powdered forms A. I. Levin and S. A. Pushkareva (*Zhur. Fiz. Khim.*, 1957, **31**, (9), 1983–1991) Cathodic reactions involved in the electrodeposition of Fe are examined by studying polarization curves at constant potential, with simultaneous determination of current efficiency. The mechanism of deposition in the compact and powdered form is examined.

Fault-finding in the plating department P. Green (*Electropl. Met. Fin.*, 1958, **11**, Dec., 432–435) Causes of rejects are considered including defects in pretreatment and after-treatment. A practical method of tracing faults is given.

Organic finishing developments of 1958 D. A. Marino (*Met. Fin.*, 1959, **57**, Jan., 48–51) References are given to methods of application, specialized coatings, wash primers and analysis and testing in the field of organic coatings (116 refs).

Plastic coatings on metals G. Besson (*Corros. Techn.*, 1958, **5**, Dec., 384; from *Industrie des Plastiques Modernes*) A brief account of the coatings used, method of application and uses of coated materials.

The protection of cargo tanks with epoxy resin-based coatings T. Y. Yang (*Corros. Prev.*, 1958, **5**, Dec., 59–61) The Chinese Petroleum Corporation, Kaohsiung Refinery, Formosa, has treated one of its railroad tank cars with an epoxy coating. Several coats were applied, the first with the brush. Curing was done by catalytic action, the temperature at which curing proceeds is about 60°F. The results reported with this coating are very satisfactory.—T.G.

Polyvinyl chloride tapes for pipe protection K. S. Chitty (*Corros. Techn.*, 1959, **6**, Feb., 39–41) The requirements from, and the properties of tapes for the protection of pipe lines – in particular of the welded joints – are reviewed and some practical applications of the tapes as well as the testing procedures are described.—T.G.

Oxide adherence and nickel flashing in vitreous enamelling H. Crystal and G. Bullock (*J. Amer. Ceram. Soc.*, 1959, **42**, Jan., 30–39) The effects of iron deposition and Ni flashing on adhesion to sheet steel were examined. Ni promotes oxide adhesion, which correlates with a TiO_2 self-opacified cover-coat, but satisfactory surface texture has not been achieved. HNO_3 pickling without Ni also promotes adhesion. The Fe content of HCl baths and the H_2 absorption influence the amount of Ni deposited in flashing (24 refs).

Adherence of enamel on cast iron A. Hauttmann (*Giesserei*, 1959, **46**, Feb. 26, 105–111) Adherence tests on cast iron were carried out with the 'Double T-test' which is described. Sixteen factors are listed and discussed which affect the adhesion of enamel to cast iron, at the same time an attempt has been made to assess the effect of these factors quantitatively.

Latest experience with one-coat white enamelling W. Tepohl (*Stahl Eisen*, 1959, **79**, Mar. 19, 356–361) One-coat enamelling is much more critical than the conventional process and depends not only on the chemical composition and physical properties of the enamel but also on the chemical composition of the steel – Ti addition to the steel is usually essential – and on the surface treatment of sheet steel. It was found that a Ni flash deposited before enamelling improved the coating so much that equally good results could be obtained on Ti-free steels.—T.G.

Lithia in porcelain enamels P. A. Huppert (*Bull. Am. Ceram. Soc.*, 1959, Feb., 57–60) The properties of enamels containing Li, the thermal coefficient of expansion of Li_2O, LiF in enamels and ceramic coatings and the advantages of complex Li compounds are reviewed.

Ceramic coatings for the control of radiant heat E. A. Douglas (*Am. Ceram. Soc. Bull.*, 1959, **38**, Jan., 20–22) A composition of low emissivity for low alloy steels coatings is described.

Control of production in the enamel shop A. K. Williams (*Found. Trade J.*, 1958, **105**, Dec. 11, 709–713) The author reviews the difficulties encountered when enamelling cast iron and gives a detailed description of the production cycle for obtaining enamelled castings of uniformly good quality.—T.G.

The painting of shot-blasted steel (*Corros. Prev.*, 1958, **5**, Dec., 51–52) The requirements of a protective coating (for at least six months) of steel plates, particularly for those used in shipyards are enumerated. The requirements are best met by a Zn-rich primer containing at least 95%Zn in the dried coat. It is pointed out that a good sealer coat must be used to prevent blistering and saponification. Sealers based on natural or synthetic rubbers are the only ones that are really satisfactory.—T.G.

Temporary protection for shot-blasted steel K. V. Hodgson (*Corros. Prev.*, 1958, **5**, Dec., 44) A new primer for temporary protection of steel surfaces particularly after shot blasting, e.g. for the recently installed shot blasting plants in shipyards, is described. The properties of the steel plates so treated are discussed, it is indicated that the primer interferes neither with the welding of the plates nor with subsequent cathodic protection.—T.G.

CLAD SHEET AND HARD-FACING

Nickel-clad steel and monel-clad steel (*Wiggin Nickel Alloys*, [1959], (51), 3–9) Production is outlined, mechanical properties and fabrication and welding methods are given and available dimensions are tabulated.

The joining of clad metals and lining techniques H. Gerbeaux (*Sond. Techn. Connex.*, 1958, **12**, Nov./Dec., 441–459) The applications of clad steel are discussed, various types of

ferrous and non-ferrous materials used are tabulated, welding techniques described and some examples of fabrication in clad steel given.

New tubing fights corrosion J. B. Ulam (*Iron Age*, 1959, **183,** Feb. 19, 123–125) Composite tubing with dissimilar metals for jacket and lining is produced by a hot extrusion process which results in true molecular bonding between oxide-free surfaces. The mechanical and physical properties of such tubing are described.—A.G.

Some experiences in the use of 'Zero-Mist' in hard chromium plating V. Massuet (*Met. Fin. J.*, 1958, **4,** Dec., 467–473) Work in Spain is described. Structures are shown and mechanical properties are given. Appearance is also assessed. The nature of 'Zero-Mist' is not stated.

Hardfacing by rod and metal powders E. Wallace (*Weld. Met. Fabr.*, 1959, **27,** Jan., 5–13) A review of old and new methods for ferrous and other metals. Properties of typical alloys are given and structures are shown. Various uses are referred to and parts are shown.

Find new uses for heavy plate with built-in clad channels (*Iron Age*, 1958, **182,** Dec. 4, 133–135) Steel plates chanelled with a system of ducts or grooves clad with stainless steel or corrosion or heat-resistant metals are used for heat transfer and similar purposes.

How welding combats chemical corrosion F. Schneider and R. J. Walker (*Canad. Metalw.*, 1958, **21,** Dec., 34–40, 46) Experience with overlay welding of stainless steel to mild-steel sulphate digesters in the paper industry is reported. The overlay is deposited at an effective rate of 1·5 ft²/h at a welding speed of 36 in/min. The overlay contains about 19%Cr, 16%Ni and 0·65%Mn. An A-arc process is employed. Examinaton after 18-months' service showed little or no corrosion.—T.G.

Diffusion coatings for metals R. L. Samuel (*Met. Fin. J.*, 1959, **5,** Jan., 5–11, 28) A brief discussion of the nature of diffusions, especially at solid surfaces, and of surface reactions is followed by an account of chromizing, the process and the properties of the coatings produced, and of sherardizing and calorizing (34 refs).

Plasma-arc plating R. D. Stackhouse, A. E. Guidotti, and D. M. Yenni (*Prod. Eng.*, 1958, **29,** Dec. 8, 104–106) A method of hardfacing with sprayed high-melting metals and W-refractory oxide combinations is described.

Plasma arc torch ushers in new fabricating, coating methods (*Iron Age*, 1958, **182,** Dec. 4, 136–137) This new process deposits high temperature metallic or non-metallic coatings. Examples are W deposits on graphite, or W parts deposited on a brass former which is subsequently dissolved. The torch is a small device in which a stream of extremely hot ionized gas is formed by an arc; the substance to be worked is fed in powder or wire form and being made fluid or plastic by temperatures in excess of 15000°F is deposited as required. The equipment cannot be purchased but parts are being made to order.—D.L.C.P.

Carbide impregnation Wickman Ltd (*Aircraft Prodn.*, 1959, **21,** Mar., 114–116) Cutting tools and other parts subject to wear can be faced with sintered carbide, increasing life two to four times.

Carbide impregnation extends HSS tool life (*Metalw. Prod.*, 1959, **103,** Feb. 6, 234–236) An account of the Wickman Impregnator for applying sintered carbide layers to drills, mills, and dies. Examples of output from treated tools are given.

POWDER METALLURGY

Electrodeposition of powders for powder metallurgy C. L. Mantell (*J. Electrochem. Soc.*, 1959, **106,** Jan., 70–74) General principles and properties are given and the metals are considered separately, with a large section on iron.

Metal powder rolling (*Mech. Eng.*, 1958, **80,** Mar., 100) A note on the direct mill compaction of powder to strip developed by the Stanat Mfg. Co., Westbury, N.Y.

Growth of sintered metal compacts J. E. Elliott (*Metallurgia*, 1959, **59,** Jan., 17–27) The results are described into an investigation of factors influencing the growth occurring when some metal powder mixtures are sintered. Fe-Cu mixes are discussed.

For lower costs and unique properties, sinterings are the choice (*Prec. Met. Mold.*, 1959, **17,** Jan., 43) Production costs are lower and properties better in an example of a sliding vane pump cylinder made from sintered iron by General Electric Co., when compared with the same part made as an iron casting.

Furnace sintering of metals and ceramics. I R. L. Harper (*Ind. Heating*, 1958, **25,** Aug., 1530–1532, 1534, 1536, 1538) Furnaces for sintering metal powders are briefly discussed and the products shown.

FERRITES, CERMETS AND CARBIDES

Reduction of nickel ferrite by graphite V. N. Bogoslovskii, M. G. Zhuravleva, and G. I. Chufazov (*Doklady AN*, 1958, **123,** (1), 87–89) Nickel ferrite was prepared by sintering an equimolecular mixture of Fe_2O_3 and NiO in air at 1200°C for 30 h. Mixed with graphite it was subjected to reduction at 950°C. The velocity of reduction, at first low, reached its maximum at 80% reduction. The product of reduction, in the early stages, was almost pure Ni, followed by wüstite. At 70% reduction a solid solution of Ni on Fe, having a body-centred lattice, appeared. Graphs showing the changes of lattice parameters of the metallic and the ferrite phases against the degree o reduction, are given. It is concluded that the mechanism of reduction is concerned both with surface layer ionic diffusion and by a deep body diffusion.—S.L.T.

Design parameters for cermets J. C. Redmond (*MET* 365–574; *PB* 131783–80, 1956, Dec., 70–87) Due to the relatively low toughness of cermets, accurate information on this property is essential. The author describes work carried out at Kennametal Inc. to evaluate means for evaluating TiC cermets with regard to their likely performance.

PROPERTIES AND TESTS

The examination and testing of metallic materials by physical methods H. Thompson (*J. Inst. Automobile Assessors*, 1959, **10,** Jan., 28–52) A survey reviewing sulphur prints, ink prints, macro-itch tests, lead exudation, and upset and step tests on steel. Metallographic examination, grain size measurement, mechanical tests, and hardness measurements are then considered, the latter at length.

Research life-blood of the steel industry J. S. Walker (*Steel Rev.*, 1959, Jan., 10–21) A general account of some investigations in progress is given.

Materials evaluation in relation to component behaviour. Proceedings of the Third Sagamore Ordnance Materials Research Conference 1956 (*MET* 365–574; *PB* 131783–800, pp.613) **The evaluation of steels** A. O. Schaefer (2–15) Methods of assessing steel are outlined; methods of sampling and testing are discussed, the inadequacy of the tensile test is pointed out, notch bar impact tests are described and the problem of brittle fracture reviewed.

Plasticity and fracture R. Beeuwkes jun. (89–154) After discussing the limitations of mechanical tests in assessing service life, the author examines the interpretation of metal quality in terms of stress–strain–fracture characteristics, and discusses the effect of notches and the mechanism of crack growth.

A survey of the low-temperature brittle fracture problem J. D. Lubahn (154–191) The problem of brittle fracture and the behaviour of mild and alloy steels are discussed, theories of failure by brittle fracture are considered, and the use of notched tensile tests and impact tests in design is examined (21 refs).

Notch-tension testing E. J. Ripling (207–233) Variables influencing the notch properties of steels and non-ferrous metals are discussed. For face-centred cubic metals, notch ductility was found to be a linear function of the strain-hardening exponent (16 refs).

Bucket failure mechanisms and problems in correlation of laboratory data with gas-turbine bucket performance J. W. Weeton, G. M. Ault, and F. B. Garrett (235–263) Correlation between laboratory fatigue data and fatigue life has been attempted, but in general fatigue life of buckets cannot be predicted. The scope of such tests in design is considered.

Material properties and their use in design M. J. Manjoine (264–301) The problems which face the designer are considered, and the lines on which the research scientist and engineer can collaborate are indicated (30 refs).

Prediction of creep and creep buckling of thin symmetrical shells from tensile creep data C. C. Bigelow (279–301) The prediction of creep deformation is axially symmetrical thin shells, using the isochronous curves is discussed, and creep buckling is predicted by plotting isochronous stress-strain curves from elongation–time curves, constructing isochronous tangent modulus curves from them, and converting to isochronous reduced modulus curves by the formula:

$$E_r = \frac{4\,EE_t^2}{(\sqrt{E} + \sqrt{E_t})^2}$$

Application of laboratory fatigue data to gear design D. W. Dudley and W. F. Diffin (337–354) Experiments are described with the object of simulating service performance by correlating the endurance limit of steels as measured by laboratory rotating beam fatigue tests, laboratory gear tooth specimen fatigue tests, and gear endurance in actual service.

High-temperature properties in relation to design J. R. Weir, R. V. Meghreblian, and D. A. Douglas jun. (441–457) The strain-cycling and relaxation characteristics of ductile metals are discussed, together with other physical and mechanical properties which relate to the design of high-temperature systems. Measuring techniques are discussed and some data on Inconel at 1500°F are reported. Design criteria for high-temperature work are discussed.

Studies of hydrogen embrittlement produced by electrolysis A. Brenner and G. B. Wood (476–495) This is a progress report on researches sponsored by the National Advisory Committee for Aeronautics and Springfield Armoury. Hydrogen isotopes were used to investigate the mechanism of hydrogen embrittlement, and in addition plating processes have been studied which do not cause hydrogen embrittlement.

Test methods for evaluating hydrogen embrittlement G. Sachs (496–513) The effect of various factors on hydrogen embrittlement is outlined, and some comments on the general significance of test data are followed by a brief review of test methods available.

Tests for the evaluation of weldments L. R. Jackson (518–544) The theory underlying the interpretation of tests to evaluate weldments is discussed and test methods are described and compared. The concept of 'transition temperature' is referred to, and the use and limitations of the Charpy V-notch and other impact tests are reviewed (30 refs).

V-Notch Charpy impact testing welded joints C. E. Hartbower (557–581) Factors causing unreliable results in the V-notch Charpy test for welded joints are discussed. The method is satisfactory if due consideration is given to selecting a significant criterion of performance and definition of transition temperature. Techniques employed are discussed (19 refs).

Stress-rupture behaviour of weldments and brazed joints J. G. Sessler (582—600) A review is given of some methods used to adapt the stress-rupture test for assessing weldments and brazed joints. Experimental data are examined and the advantages and limitations of the methods described are discussed briefly.

Statistical mechanics and metallurgy C. Zener (*Z. Metallkunde*, 1959, **50,** Jan., 3–7) Examples of the importance of statistical mechanics in metallurgy are given; these include the influence of internal stress on the modulus of elasticity and of magnetic entropy on the polymorphic transformatons of Mn and Fe.

Nitrogen in ferritic steels: A critical survey of the literature W. C. Leslie (*AISI Contrib. Met. Steel*, 1959, Feb., pp.141).

Solubility of hydrogen in stainless steels R. Blanchard (*Compt. Rend.*, 1959, **248,** Feb. 16, 966–968) The solubility of H_2 in 18–8 steels varies from 5·3 c.c./100 g at 700°C to

7·25 c.c. at 1000°C. Additions such as Mo and Nb slightly lower this solubility.—A.G.

On the function of nitrogen as an alloying element in heat-resisting materials. IV. The effect of carbon content on the nitrogen absorption of 20% Cr–Fe alloys O. Miyakawa (*Tetsu to Hagane*, 1958, **44**, Sept., 1137–1139).

The relation of hydrogen content in liquid iron with water-vapour pressure in the atmosphere C. Yoshii (*Tetsu to Hagane*, 1958, **44**, Sept., 1069–1070).—K.E.J.

On the behaviour of hydrogen in molten steel. III. On the behaviour of hydrogen in molten steel during the oxidizing period of the basic electric arc furnace process T. Sugiyama (*Tetsu to Hagane*, 1958, **44**, Sept., 1041–1043).

How nickel affects high-speed tool steels R. F. Hehemann and A. R. Troiano (*Iron Age*, 1958, **182**, Dec. 18, 94–96) Possible benefits from adding nickel to tool steels are listed. The effects of nickel on the annealing of such steels are described.—A.G.

Niobium-treated mild-carbon steel (*Mech. Eng.*, 1959, **81**, Jan., 53) A series of steels known as GLX–W produced by Great Lakes Steel is announced. Properties are given and structures shown.

Molybdenum effect on grey iron elevated-temperature properties G. K. Turnbull and J. F. Wallace (*Mod. Castings*, 1959, **34**, Jan., 81–92) Mo additions of up to 1·5% produce a considerable and consistent improvement in the short-time UTS from room temperature to 1200°F of a grey iron with 3·8%C. Up to 2%Mo increases the stress-rupture properties at the same temperatures. With increasing temperature the strength values decrease regularly owing to the decomposition of the matrix. 0·6%Cr addition to a Mo-bearing iron improves some of the properties, whereas others deteriorate. The effect of Mo on the structure of the iron is shown (20 refs.).—T.G.

The role of molybdenum in abrasion-resisting materials (*Climax Molybdenum pamphlet*, 1959, pp.32) Various materials, including Mo steels, are evaluated and their structures and properties are shown.

Effect of V and other elements on Si–Mn high-strength structural steel E. Asano (*Tetsu to Hagane*, 1958, **44**, Sept., 1116–1118) The effects of V content and of heat treatment on the mechanical properties are shown, and the effects of Zr, Cr, Ti, and C are also considered.

The relation between the quality of rimmed steel for tubes and internal defects thereof. III. Effect of sulphur in steel on the internal defects of rimmed steel tubes N. Eguchi (*Tetsu to Hagane*, 1958, **44**, Sept., 988–990) The relationships between number of twists to fracture and S content and Mn/S ratio, and between cracks on the surface of the twist specimen and S content, are investigated.—K.E.J.

Columbium-treated steels, low cost, high strength F. W. Starratt (*J. Metals*, 1958, **10**, Dec., 799) Development of Nb-treated steels by the Great Lakes Steel Corp. is outlined.

The comparison between American and ordinary mild steel plates K. Yoshizaki, T. Ando, T. Oyama, and S. Chikamitsu (*Toyo Kohan*, 1952, Nov., 8–17) [In Japanese] Cold-rolled, Al-killed mild steel sheets from American and Japanese makers are compared after annealing. An American batch had elongated grains in the recrystallization structure and better mechanical properties.—K.E.J.

Progress in stainless and heat-resisting steels J. Lomas (*Eng. Boiler House Rev.*, 1959, **74**, Feb., 54–55) Various steels described during the year are briefly noted.

New stainless better than type 316 (*Chem. Eng.*, 1958, **65**, Dec. 15, 194, 196) The properties of stainless D319 with 17·5–19·5%Cr, 11–15%Ni, 2·25–3%Mo and (maxima), 0·07%C, 2·0%Mn, 1·0%Si, 0·045%P, and 0·030%S are given. This has been approved by ASME Boiler and Pressure Vessel Committee. Much of the 316 alloy steel used falls within this composition and these have proved satisfactory for as much as 15 years.

A contribution to the selection of spring steels Y. Yamaki (*Tetsu to Hagane*, 1958, **44**, Sept., 1093–1095).—K.E.J.

The effect of niobium on the high temperature properties of austenitic stainless steel T. Tauchiya (*Tetsu to Hagane*, 1958, **44**, Sept., 1132–1134). Relationships are shown between Nb/C ratio and hot-workability, hot tensile strength, and hot impact value. The effects of Nb content on hot elongation and hot impact value are also shown.—K.E.J.

Study on the 13Cr–Al stainless steels K. Yoshida (*Tetsu to Hagane*, 1958, **44**, Sept., 1128–1130) Data are given for quenching and tempering hardness, the effects of heat treatment on mechanical properties, and microstructure after heat treatment.—K.E.J.

Poor-man's alloy may replace stainless steel in aircraft (*Mach. Design*, 1959, **31**, Jan. 8, 6) A new Al-containing austenitic steel 15% lighter than 18–8 stainless is referred to.

Research and development at Corby (*Brit. Steel*, 1958, **24**, Dec., 392–394) An account of the new research department at Stewarts and Lloyds.

Institute of ferrous metallurgy in Gliwice T. Malkiewicz (*Technik*, 1959, **14**, Feb., 114–115) The organization of the Gliwice Institute and its range of activities are surveyed.

METALLOGRAPHY

Phase-contrast techniques in metallurgical microscopy G. Koves (*Eng. Dig.*, 1959, **20**, Mar., 110–112, from *IBM J. Res. and Development*, 1959, **3**, Jan., 85–92) Uses in the identification of constituents with practical examples are given.

Rapid quenching hot stage for metallography G. R. Speich and M. Cohen (*Rev. Sci. Inst.*, 1958, **29**, Dec., 1129–1131) A stage operating up 1100°C *in vacuo* or in an inert atmosphere and fitted with a cold He supply for quenching at 500°/sec is described. Some results with bainite are shown.

Microfurnace for thermal microscopy and studies at high temperatures W. A. Lambertson and G. Lewis (*J. Electrochem. Soc.*, 1959, **106**, Feb., 124–126) A furnace working up to 3000°C is described. Heating is by resistors and temp. measurement by optical pyrometer. It has been used to measure the m.p. of Al_2O_3, ZrO_2 and other refractory oxides.

X-Ray projection microradiography F. W. von Batchelder (*Trans. Met. Soc., AIME*, 1958, **212**, Dec., 798–799) A note describing the principles of X-ray projection microradiography by a fine-focus tube. A magnification of ×50 was obtained at a distance of 1 m.—T.G.

The determination of textures in metallic substances using counter tubes F. Haeszner (*Metall*, 1958, **12**, Feb., 89–95; Dec., 1094–1101) Texture is defined as crystallographic grain orientation, and the use of counter tubes instead of photographic methods for X-ray examination is described. Methods of representation of the results are given including the pole figure method. Analysis and quantitative estimations of the data are considered and the necessary formulae are derived. The practical details of measurement are given in the second part (54 refs.).

Simple autoradiographic technique T. K. Bierlein and B. Mastel (*Rev. Sci. Inst.*, 1959, **30**, Jan., 46–47) A method of making a plastic replica coated with a liquefiable photographic emulsion is described.

A universal electrolyte for the polishing of stainless and heat-resisting steels, low-carbon steel, and titanium alloy VT-5 G. I. Parfessa and V. A. Sidlyarenko (*Avtom. Svarka*, 1958, **11**, (7), 83–84) A method particularly adapted to welds in stainless steels has been worked out. An acetic-perchloric composition taking 120 volts at a c.d. of 0·3–0·25 amp/cm² is used.

Small size instrument for electropolishing and electroetching of microsections L. Ya Popilov (*Zavod. Lab.*, 1958 (7), 890–892).

Cast iron with diffuse annealing graphite J. Pomey (*Rev. Mét.*, 1958, **55**, July, 627–649) An investigation into the formation and development of transitional carbides from martensite and bainite is described. A study of graphite growth from the crystallographic and kinetic aspects is presented.—B.G.B.

Iron containing diffused temper graphite physico-chemical and metallographic aspects J. Pomey (*Rev. Univ. Min.*, 1958, **14**, Sept., 293–300) An investigation is reported into the mechanism of the development of transitional carbides from martensite and bainite and the nucleation of graphite from hexagonal carbide. Graphite growth is studied from the points of view of morphology and kinematics, and the treatment and mechanical properties of these irons are discussed.

An electron-diffraction method for determining the crystal lattices of small thin lamellar precipitates occurring in steels G. R. Booker, J. Norbury, and A. M. Rose (*JISI*, 1959, **193**, Nov., 221–226) [This issue].

Modification of the cast structure of high-speed steel G. Hoyle and E. Ineson (*JISI*, 1959, **193**, Nov., 254–269) [This issue].

Studies on changes in state of iron-vanadium alloys K. Bungardt and W. Spyra (*Arch. Eisenhüt.*, 1959, **30**, Feb., 95–102) The object of the investigation was to determine whether similar changes in state occurred in Fe–V alloys at temperatures about 500°C as in Fe–Cr alloys with more than 13%Cr, which changes, in the latter, lead to embrittlement at 475°C. Fe–V alloys with 14, 27, and 45%V were studied at temperatures from 350° to 1100°C. Hardness, magnetization, electrical resistance, thermal expansion coefficient, and the modulus of elasticity were measured.—L.J.L.

On the structure control of forged steel K. Kunii (*Tetsu to Hagane*, 1958, **44**, Sept., 1120–1122) Good structures, as judged by austenitic grain size, are achieved with Al contents exceeding 0·015%.—K.E.J.

Hexagonal carbide and nitride phases and their occurrence in some alloy steels K. W. Andrews and H. Hughes (*JISI*, 1959, **193**, Nov., 304–311) [This issue].

Fundamental study on heat-resisting cast iron. I. Relations between microstructures and hardness and contents of chrome and silicon Y. Kawano (*Tetsu to Hagane*, 1958, **44**, Sept., 1006–1007).—K.E.J.

Solubility and precipitation of vanadium in alpha- and gamma-iron R. W. Fountain and J. Chipman (*Trans. Met. Soc., AIME*, 1958, **212**, Dec., 737–748) With a Sieverts-type apparatus for determining the solubility of N_2 in solid Fe and in Fe–V alloys, a study has been made of the conditions under which the nitride VN precipitates from the solid solution. From the results of the studies, which are reported and discussed in detail, it is concluded that small additions of V should be effective in eliminating strain-ageing due to N_2 in steel provided the annealing process is such as to initiate the precipitation of the nitride (29 refs.).—T.G.

Precipitation and dissolution in the system copper–iron V. Schumacher (*Z. Metallkunde*, 1959, **50**, Feb., 81–86) Dissolution and precipitation phenomena in the system Cu–Fe over the range 0·03–1·19% Fe were studied by resistance and thermo-e.m.f. measurements as both are extremely sensitive to very small amounts of Fe in solid solution. It was found that Fe is present as metallic Fe in a very finely dispersed form after quenching from the solid solution range. During ageing in the two-phase field, part of this Fe is re-dissolved. The phenomena are explained by the dislocation theory.—T.G.

The nature of the solid solution of boron in alpha-iron A. K. Shevelev (*Doklady AN*, 1958, **123**, (3), 453–456) Quenched and annealed samples of B–Fe alloys were studied by means of X-rays. The results show that on solution of B in α-Fe there is a decrease of the lattice constant, suggesting the formation of a replacement solid solution.—S.I.T.

CORROSION

New knowledge on the corrosion and protection of iron and steel: A review of literature published in 1954–55 H. Steinrath and H. Ternes (*Stahl Eisen*, 1959, **79**, Feb. 19, 216–222) A review with 196 references arranged under the headings: Reviews on the whole field of corrosion; theoretical studies; corrosion testing; effect of welding, heat-treatment and working.—T.G.

Influence of degree of stabilization on precipitation and intercrystalline corrosion in a high-temperature niobium-stabilized steel with 16%Cr, 13%Ni, molybdenum and vanadium K. Bungardt and G. Lennartz (*Arch. Eisenhüt.*, 1959, **30**, Jan., 23–29) The precipitation

behaviour of the high nitrogen steel X 8CrNiMoVNb16.13, was followed by chemical analysis and radiographic examination of isolated residues. In the quenched condition a mixed carbonitride [(Nb, V) (C,N)] was detected, the V content of which increased with increasing Nb content and rising quenching temperature. Rate of precipitation is increased by cold working before annealing. The effects of ageing for 5 h at 750°C before annealing are discussed, and the results of investigating grain breakdown as a function of annealing temperature and time are examined.

Consider nonoxidative corrosion R. V. Jelinek (*Chem. Eng.*, 1958, **65,** Dec. 29, 56–60) A review of attack by liquid metals. Attack on iron and stainless steel by molten Li is shown.

Solid metal–liquid metal reactions in bismuth and sodium J. R. Weeks and D. H. Gurinsky (*Liquid Metals and Solidification, a Seminar during the 39th National Metal Congress and Exposition, Chicago, Nov.* 1957, *sponsored by the ASM*, 1958, 106–163) Reactions of Bi with Fe, grain boundary attack, metal transfer, effects of Ti and Zn and solubility effects are included in a study which was mainly non-ferrous. Solubility effects of Fe–Cr in liquid Bi are also investigated.

Some fundamental features of mechanochemical attack on metals T. P. Hoar (*NATO, Adv. Group for Aeronaut. Res. and Dev. Report* 158, 1957, Nov., pp.5) The behaviour of oxide films under fretting, fatigue, static stress and chemical breakdown conditions is discussed. Disarrayed surface zones lead to less effective oxide films and without these to accelerated attack. Metal yielding under mechanical stress is chemically active. The implications for stress-corrosion cracking are considered.

The fight against corrosion (*Echo Mines*, 1959, Feb., 93) Coke and coal waggons have been constructed of ordinary copper steel, C–Mn–Cu steel, and Cor–Ten, a low-alloy steel. After 10 years' service Cor–Ten steel is claimed to be 2–3 times more resistant to atmospheric corrosion than copper steel.—A.G.

Nitric acid corrosion of austenitic stainless steels J. M. West (*J. Appl. Chem.*, 1959, **9,** Jan., 1–10) Tests in 30%HNO_3 of 11 Ti-stablized 18–8 steels before and after 2 h treatment at 650°C were made by two methods, that of Heeley and Little corresponded more closely to plant conditions. Steels with 0·06–0·1%C can be used if the Ti : C ratio is 6·0–7·2 allowing for N content but with >0·1%C. This ratio must be ~5·5. The corrosion rate rises in the first fortnight and then becomes steady, apparently due to the thickness of the exfoliated layer which dissolves as fast as it is formed.

Corrosion of metals in tropical environments. III. Underwater corrosion of ten structural steels B. W. Forgeson, C. R. Southwell, and A. L. Alexander (*NRL Report* 5153, 1958, Aug. 8, pp.24) An 8-year exposure trial is reported and various conditions of immersion and similar tests in other localities are compared. The Canal Zone tests, here discussed, were made with mild steel with millscale, pickled and machined and underwater tests on plain C, Cu-bearing, Ni–Cr and low-alloy steels.

Cable corrosion and its prevention A. Zastera (*Stahl Eisen*, 1959, **79,** March 19, 369–371) The author distinguishes between external and internal corrosion of cables and deals mainly with the latter. Internal corrosion is caused by the fibres used for making up the cable or more precisely by their humidity and content of chloride ions. Means of preventing corrosion due to these two causes are discussed.—T.G.

Corrosion problems with pressure vessels H. Allen (*Corros. Techn.*, 1958, **5,** Dec., 390, 397) A brief review of corrosion and erosion problems, safety measures and inspection.

Corrosion of metals by vapours from organic materials V. E. Rance and H. G. Cole (*Admiralty and Min. of Supply Inter-Service Metallurgical Research Council*, 1958, pp.25) Instances of corrosion are tabulated and laboratory tests on the effects of plastics, wood, acetic acid vapour, CH_2O, H.COOH in humid surroundings are reported and work in progress is briefly indicated. The metals concerned are mainly non-ferrous but mild steel and Cd plated steel are included.

Some aspects of refinery corrosion due to crude oil P. W. Sherwood (*Corros. Techn.*, 1959, **6,** Jan., 10–12) The running and maintenance items of an oil refinery which are directly attributable to corrosion or its prevention are considered. The factors contributing to the severity of corrosion and means of control are discussed.—A.G.

Corrosion cracking under tension of austenitic stainless steels in aqueous chloride solution T. P. Hoar and J. G. Hines (*Mét. Corros. Ind.*, 1959, **34,** Jan., 36–47) Stress corrosion cracking of 18–8 steel wire was investigated in a boiling concentrated solution of $MgCl_2$. As shown by measurements of electrode potential, mechanical properties and elongation of the wire taking a d.c. charge, there is a period of induction during which the process is not appreciably affected by stress, followed by a period of rapid crack propagation. Normal rate of crack propagation is 0·5–3 mm/h; not greatly influenced by applied charge and temperature, it can be inhibited by suitable cathodic protection. The results are examined, and a theory accounting for them is developed.

The effect of some inhibitors on the formation of cracks in metals by corrosion S. A. Balezin, V. V. Romanov, and N. I. Podobaev (*Doklady AN*, 1958, **123,** (5), 902–905) This is the study of the influence of certain inhibitors on austenitic stainless steel and brass under the action of certain chemical substances. It is concluded that organic inhibitors can prevent completely the intercrystalline cracking in steel subjected to the action of $MgCl_2$ solution, and that organic inhibitors introduced into lubricants can prevent the formation of cracks in brass.—S.I.T.

Some observations on the mechanism of inhibition A. C. Makrides (*J. Electrochem. Soc.*, 1959, **106,** Jan., 7–9) HCNS and citric acid inhibit the dissolution of Fe in HNO_3. Rate constants were calculated and it is concluded that inhibition does not depend upon poisoning of the combination reaction between adsorbed H-atoms.

The corrosion resistance of wrought iron and open-hearth steel: A literature survey I. I. Tingley and R. R. Rogers (*Dept. of Mines and Techn. Surveys, Ottawa, Inf. Circ.* IC111, 1958, Dec. 12, pp.21).

Internal cathodic protection. I. Introduction and general principles C. Plumpton and C. Wilson (*Corros. Prev.*, 1959, **6,** Jan., 31–36) The current requirements for the cathodic protection of the interior of oil tanker compartments are treated mathematically.—T.G.

Corrosion control by de-humidification (*Corros. Prev.*, 1958, **5,** Dec., 46–47) A dehumidification plant of the silica gel type and mounted aft in an oil tanker is described. The plant reduces the dew point to 40–45°F. The results of using this plant are indicated.—T.G.

How to prevent underfilm corrosion (*Steel*, 1959, **144,** Jan. 19, 66–68) A new cold phosphating process is described which renders the surface both passive and non-conducting. A separate priming coat is generally not required.—A.G.

Corrosion prevention in the bottling industry L. A. Johnson (*Corros. Prev.*, 1959, **6,** Jan., 45–49) The author deals with the practical side of corrosion prevention as it affects bottle store operators and particularly engineers. Because of the high humidity usually met in this type of plant, corrosion is mostly very severe. The maintenance of buildings is included in this review.—T.G.

Cathodic protection of shell plating J. P. O'Donovan (*Corros. Prev.*, 1958, **5,** Dec., 49–50) The author answers questions frequently asked when galvanic hull protection is discussed. The questions are: Why not protect the stern part only? What is the life of an installation? Can coating be dispensed with? Is cathodic protection equally effective with old ships?

Are cathodic protection techniques for you? P. Hart (*Chem. Eng.*, 1959, **66,** Jan. 12, 154, 156, 158) A report of a lecture on Dow Chemical Co. practice and findings. The use of Mg anodes underground is briefly described, and also on some tanks, reactors and sea-water lines where a partly protected electrode sprayed with polyvinyl chloride may economize Mg.

Pipe line protection in the Middle East – protective coating and cathodic protection practice L. R. Sparrow (*Corros. Techn.*, 1959, **6,** Jan., 13–18) Protection is achieved by an insulating coating coupled with cathodic protection. Practical difficulties in applying these forms of protection in the Middle East are discussed. **Preparing the ground and checking the results** (Feb., 42–48) The practical problems involved in the construction of groundbeds in the desert, their siting, and the siting of terminal and tank farms are discussed. The importance of regular and thorough checking of the pipeline protection – wrapping and cathodic protection – is stressed.

Cathodic protection against corrosion using zinc (*Echo Mines*, 1959, Feb., 99–101) The use of zinc to minimize corrosion by salt and fresh water and to buried structures is discussed. The cheapness of zinc and its efficiency are considered to render its use economic in many cases.—A.G.

ANALYSIS

Microscope attachment for accurate microdrilling and the removal of analytical samples from small areas A. C. Simon and D. A. Gildner (*Rev. Sci. Inst.*, 1958, **29,** Dec., 1125–1128) An air-driven microdrill is described, interchangeable with the objective, which can be adjusted to coincide with the optic axis and samples the area under the cross-hair of the ocular when the substitution is made. It can use drills as small as 0·001 in. An auxiliary stage for optical viewing during sampling is also described. The apparatus was designed primarily for an inverted microscope but can be adapted to the ordinary type.

Rapid sampling method for liquid iron W. Ruff and H. J. Wirth (*Giesserei*, 1958, **45,** Nov. 6, 700–701) The authors describe the Buchanan sampling method for cast iron and suggest as a modification of it the use of a water jet instead of an air jet for producing small iron globules. The advantage of the use of water is that the carbon content is not reduced, as often happens in the conventional drilling test or by globule formation in air.

The coulometric determination of oxygen in steel by the fusion extraction process in a current of argon K. Abresch and H. Lemm (*Archiv. Eisenh.*, 1959, **30,** Jan., 1–6) The sample is melted in a graphite crucible in a stream of A under controlled conditions, the evolved CO oxidized to CO_2 and titrated coulometrically. The standard deviation was 0·0012%O, and results agreed well with those obtained by vacuum fusion, although the method is much more rapid when a series of tests was made.

An attempt at rapid determination of the oxygen dissolved in molten steel K. Sanbongi (*Tetsu to Hagane*, 1958, **44,** Sept., 1031–1032) A magnesia cell is described. The oxygen content is estimated from the change in e.m.f. corresponding with change in oxygen potential.

Study on the cause and the effect of the glow discharge in a high-frequency heating vacuum-fusion furnace for determination of the oxygen in iron and steel H. Tokube (*Tetsu to Hagane*, 1958, **44,** Sept., 1052–1053).—K.E.J.

The influence of water vapour in the eudiometric determination of carbon in iron and steel F. Stricker and K.-H. Tytko (*Archiv. Eisenh.*, 1959, **30,** Jan., 7–9) In determining C in iron and steel by the method recommended by the Chemical Committee of VDE, it was shown that the gases in the burette are not saturated with water vapour, but that the degree of saturation is always constant.

Model 912 vacuum fusion gas analyser (*Wild-Barfield J.*, 1958, **6,** Dec., 14–15) The apparatus, designed for analysing trace impurities in steels, high-melting metals and alloys, is briefly described.—A.G.

Nitrogen in steel R. Suarez Acosta (*Inst. Hierro Acero*, 1958, **11,** Oct.–Dec., 258–262) A method is described for determining N_2 in steel by direct nesslerization, followed by an absorptiometric measurement in the Spekker spectrophotometer.

Study on silicon nitride in steel. I. Determination of nitrogen in the form of silicon nitride H. Miura (*Tetsu to Hagane*, 1958, **44**, Sept., 1047–1050) Some chemical properties of silicon nitride are given. The method of determination depends on its insolubility in halogen solutions in organic solvents and its decomposition by NaOH at high temp. Results of determinations in steel are given.

The determination of nitrides of aluminium, silicon, vanadium and chromium in carbon and low-alloy steels E. Wrzesinska (**Works of Metal. Inst.*, 1958, **10**, (3), 180–184) A review of previous methods is given. A study was carried out on preferential acid solubility and the value of Beeghly's method was confirmed for AlN. CrN can be determined by difference as it is acid – insoluble. A method for obtaining Si_3N_4 content is also given.

The control and allowable errors of routine operations of analyses of C, Si, Mn, P, S, Cu, Ni, Cr, Al, Sn, and As in iron and steel O. Kamimori (*Tetsu to Hagane*, 1958, **44**, Sept., 1045–1047) Data are given for various JIS volumetric, combustion, photometric and electrical methods.—K.E.J.

Cerimetric titration of iron using a mixed indicator W. R. Heumann and C. Allard (*Anal. Chem.*, 1959, **31**, Jan., 155–156) A letter. A comparison of warning indicators for ferrous titration is described. The indicator error due to diphenybenzidinedecasulphonic acid is one-quarter that of diphenylaminesulphonate, though the warning range is less. The combined error of the two indicators is reduced to $\sim\frac{1}{5}$ by the use of the former.

Determination of titanium, zirconium, niobium and tantalum in steels: Separations by Anion-Exchange J. L. Hague and L. A. Machlan (*J. Res. NBS.*, 1959, **62**, Jan., 11–19).

Determination of small amounts of tungsten in steel (*NBS Research Highlights, Annual Report* 1958, Dec., Misc. Publ. 226, 35) A photometric method with toluene–1 : 4–dithiol is mentioned.

On the separation of vanadium, molybdenum and tungsten by means of paper chromatography Tzou Shih-Fu and Liang Shu-Chuan (*Sci. Sinica*, 1959, **8**, Feb., 196–200) [In English] The per-acids are used and the spots are detected with tannin solution. Butanol–H_2O_2–HNO_3 mixtures give the best separation.

Rapid determination of copper and lead in irons and steels H. A. Nicolas (*Chim. Anal.*, 1959, **41**, Jan., 23) A method is proposed for the rapid determination of lead and copper by direct electrolysis. Iron is stabilized in the ferrous state by the addition of hydroxylamine hydrochloride.—A.G.

Determination of trace elements by activation analysis J. M. Dederlein and F. Lingjœrde (*Tekn. Ukeblad*, 1959, **106**, Feb., 111–114) A method of general application is described for the determination of trace elements by neutron irradiation, followed by the analysis of the characteristic γ-rays of the resulting radioisotopes with a high-resolution γ-spectrometer. The determination of Co present as impurity in a mild steel is described.

Development of atomic power and its possible applications to the steel industry H. D. Philipp (*Iron Steel Eng.*, 1958, **35**, Dec., 109–116) The uses of radioisotopes in measurement and as atomic fuels are described.

Analytical methods of determining the water content of welding electrode coatings R. Botisk (*Schweisstechn.*, 1959, **13**, Jan., 1–5) Three methods for determining total water and one for determining moisture content in electrode coatings are described.

A method for the rapid analysis of flux AN-348-A N. A. Langer and T. P. Novikov (**Avtom. Svarka*, 1956, (5), 80–83) A specific method for the determination of SiO_2, CaO, F, Mn, $Fe_2O_3 + Al_2O_3$, PO_4 and S in this welding flux, taking only 2–3 h is described. Gravimetric, colorimetric and volumetric processes are used.

Determination of magnesium in slag and dross by flare photometry J. Coulombeau and M. Maire (*Chim. Anal.*, 1959, **41**, Jan., 3–11) The method described employs the line Mg 2852·1. Silica has to be removed but not oxides. The other common elements in slags interfere only slightly and an accuracy of ±0·2% is claimed.—A.G.

Study on the chemical analysis of basic slag. I. Determination of SiO_2, total iron, Al_2O_3, MnO, CaO and MgO S. Wakamatsu (*Tetsu to Hagane*, 1958, **44**, Sept., 1067–1069).—K.E.J.

Determination of sulphur in a slag that contains fluorine. Combustion method, chelatometry T. Miyate (*Tetsu to Hagane*, 1958, **44**, Sept., 1050–1052).—K.E.J.

International methods of sampling and analysis of solid fuels J. G. King (*Coke Gas*, 1958, **20**, Nov., 461–464) A review of the proceedings of the Harrogate Conference, June 1958.

A gravimetric method for analysing blast-furnace top gas D. J. Kusler (*US Bur. Mines Rep. Invest.*, 5452, 1959, pp.12).

Polarography of gases. Quantitative studies of oxygen and sulphur dioxide D. T. Sawer, R. S. George, and R. C. Rhodes (*Anal. Chem.*, 1959, **31**, Jan., 2–5) Mixtures containing also Cl_2, Br_2, NO_2 or HCN were used using a membrane electrode. The range 0·01–100% by volume was successfully covered.

An electrode system for dissolved oxygen D. E. Carritt and J. W. Kanwisher (*Anal. Chem.*, 1959, **31**, Jan., 5–9) An electrode functioning successfully in sea water is described.

INDUSTRIAL USES AND APPLICATIONS

Metallurgical progress and the steam engineer L. Sanderson (*Steam Eng.*, 1959, **28**, Feb., 158–160) New steels and the performance in steam plant of these and other heat-resisting steels, recent work on intergranular corrosion, and work on creep are briefly noted, also Ni–Cr cast iron for pumps carrying fly-ash slurry, and new steels and non-ferrous materials for miscellaneous uses.

Quality and manufacture of high-temperature and high pressure steam tubes K. Harada (*Tetsu to Hagane*, 1958, **44**, Sept., 1081–1083).

Knowledge gained in the last 25 years on the life of hoist shaft cables in the Ruhr district. K. Schlegel (*Stahl Eisen*, 1959, **79**, Mar. 19, 367–369. A review of data on the life of hoist cables showing that modern cables have a life four times as long as those in use in 1938. The various factors that led to this improvement are discussed and recommendations made for the proper care of the cable during service.

The influence of the properties of hot rolled tin plate on can manufacture T. Yoshimura, H. Kato, S. Asada, and K. Gokan (*Toyo Kohan*, 1952, Nov., 28–41) [In Japanese] In respect of cracking of cans through brittleness, semi-killed steel is better than rimmed, and steel from the top of a rimmed ingot is worst. Brittleness is caused by inclusions and pipe, surface defects, coarse grains and irregular grain boundaries.—K.E.J.

Materials of construction: High temperature metals E. Skinner (*Chem. Eng.*, 1958, **65**, Dec. 15, 137–176) Cast irons, steels including cast stainless (6 types) and Ni-base and super-alloys are considered and creep and other high-temperature properties are given, also oxidation resistance. The materials are considered for chemical plant construction handling a variety of aggressive materials including liquid metals. A directory of manufacturers is added (pp.148–176) covering all sorts of metallic and non-metallic materials of construction.

The use of steel grids in tower purifiers (*Coke Gas*, 1959, **21**, Mar., 118–120) An account of the experience of the West Midlands Gas Board is given.

Manganese steel in industry (*Mech. World*, 1958, **138**, Dec., 543–544) Heavy-duty parts requiring manganese steel are mentioned with notes on the wear and resistance properties and advantages of the material.

Use of stainless steel in the oil industry (*Iron Steel*, 1959, **32**, Feb., 75–76) The use of ferritic, martensitic and austenitic stainless steels for parts resistant to high pressures and temperatures and corrosive atmospheres is discussed.

Low-carbon Mn–Cr–Mo–V steel for pressure vessels I. M. Mackenzie (*Chem. Proc. Eng.*, 1959, **40**, Apr., 132–136) The development of the steel, known as Ducol W30 is described and its properties are given including weldability, and uses are noted.

Revision to published chart: Metals for high-pressure hydrogenation plants G. A. Nelson (*Trans. ASME.*, 1959, **18B**, Feb., 92–93) Low-carbon and 5 alloy steel types are shown.

Grain-oriented electrical steels B. D. Jenkins (*Elect. Times*, 1959, Jan. 8, 48–51; Jan. 15, 102–104) A review of toroidal and C-cores and of power transformer applications in single and three-phase circuits.

Structural materials for supersonic aircraft N. F. Harpur and I. L. G. Baillie (*Aeroplane*, 1959, **95**, Feb. 27, 244–247) Temperatures expected and the selection of materials are reviewed. 18–8 steel is included.

HISTORICAL

A fruitful year in British ironmaking M. Schofield (*Iron Steel*, 1959, **32**, Feb., 51–52) An historical note, dealing with the founding in 1759 of the Carron ironworks and the New Willey Company.

Women's labour in the early English iron industry H. R. Schubert (*JISI*, 1959, **193**, Nov., 253) [This issue].

The first industrial production of ingot steel D. Carlberg (*Konvertern*, 1958, (3/4), 26–32) An account of early work in the manufacture of converter steel in Sweden in the mid-19th century, at Edsken and Högbo.

Ingot steel production in the past 100 years T. Wahlberg (*Konvertern*, 1958, (3/4), 34–38) A brief review of the development of steel-making processes during the last century.

Consett of Durham E. B. Thomas (*Steel Rev.*, 1959, Jan., 22–31) Historical.

Tungsten and titanium in steel: Centenary of Robert Mushet's invention (*Met. Treatment*, 1959, **26**, Feb., 52) An historical note.

James Carmichael, 1776–1853 E. N. Simons (*Brit. Steel*, 1959, **25**, Jan., 22–23) This is an account of the career of the Scottish engineer and his brother Charles, his invention of the fan blowing machine, and the contributions of the brothers to ship and locomotive construction.

World history in Gästrikland P. Carlberg (*Konvertern*, 1958, (3/4), 18–20) A brief historical review of the development of iron and steel making.

Alexander Parkes – pioneer of plating M. Schofield (*Prod. Fin.*, 1959, **12**, Apr., 65–67) A centenary appreciation.

ECONOMICS AND STATISTICS

World steel expansion slows down (*Steel Rev.*, 1959, Apr., 7–9) Statistical.

Steel in Scotland (*British Iron and Steel Federation Pamphlet*, 1959 (revision) (pp.8) Mainly statistical.

Steel in the German economy: A Comparison with Britain (*Steel Rev.*, 1959, Apr., 10–25).

The awakening dragon (*J. Met.*, 1959, **11**, Mar., 182–183) A report is given of the industrial growth of China during the past few years.

MISCELLANEOUS

Semi-reflecting glass gives infra-red protection (*Eng. Mat. Design*, 1959, **2**, Feb., 77) The use of Polyverbel glass produced in Belgium for protection against radiation from furnaces in the iron and steel industry is indicated.

Magnetic and electrical anisotropies of iron sulphide single crystals E. Hirahara and M. Murakami (*J. Phys. Chem. Solids*, 1958, **7**, Dec., 281–289).

The variation with temperature of the catalytic activity of an oxidized stainless steel surface in the oxidation of sulphur dioxide R. S. Fielder, P. J. Jackson, and E. Raask (*Fuel*, 1959, **38**, Apr., 257–258).

Heat capacities and thermodynamic properties of the iron tellurides $Fe_{...}Te$ and $FeTe_2$ from 5° to 350° K E. F. Westrum jun., Chien Chou, and F. Grønvold (*J. Chem. Phys.*, 1959, **30**, Mar., 761–764).

Profile: Sir Andrew McCance (*New Sci.*, 1959, **5**, Feb. 26, 466–467).

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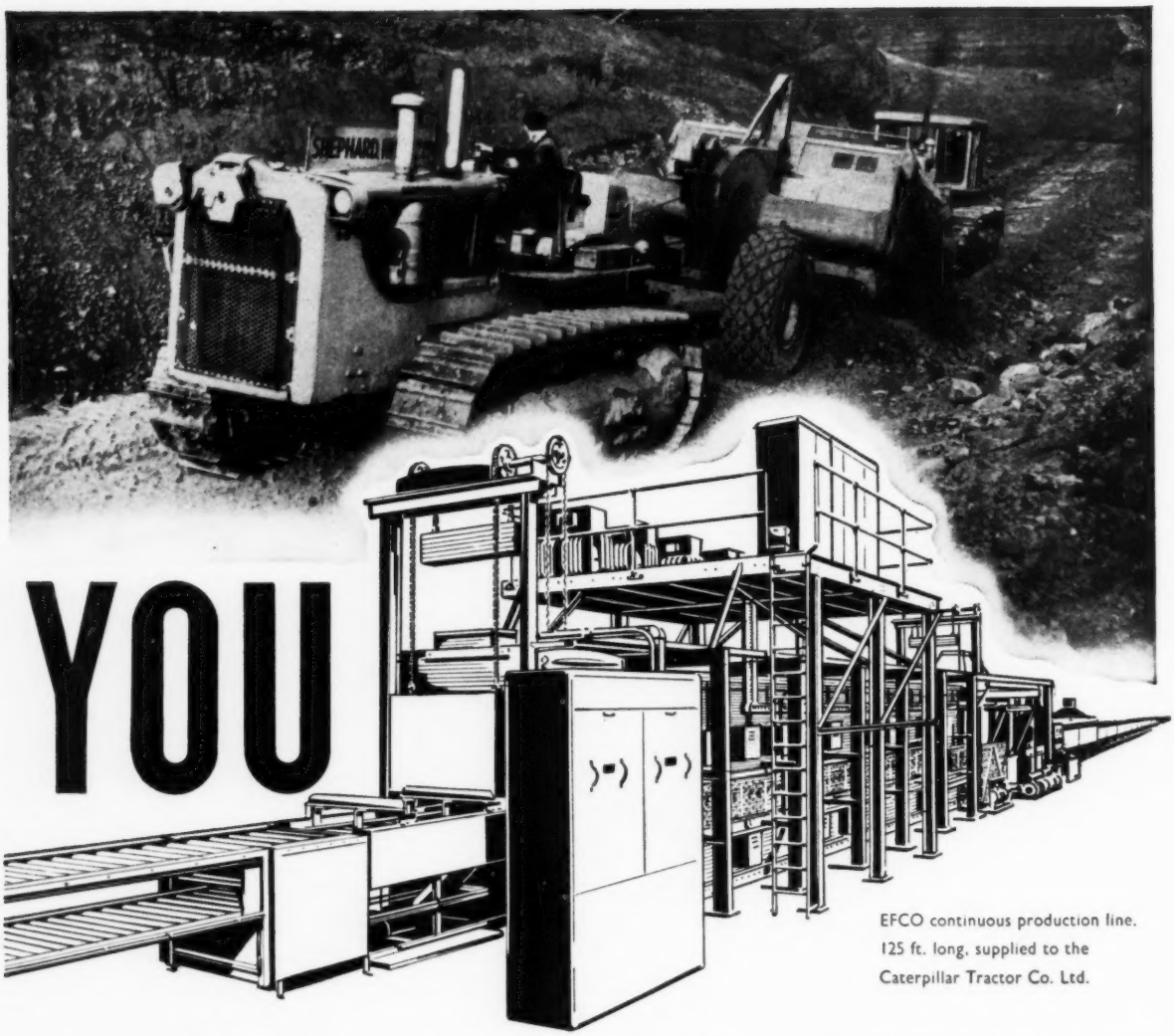

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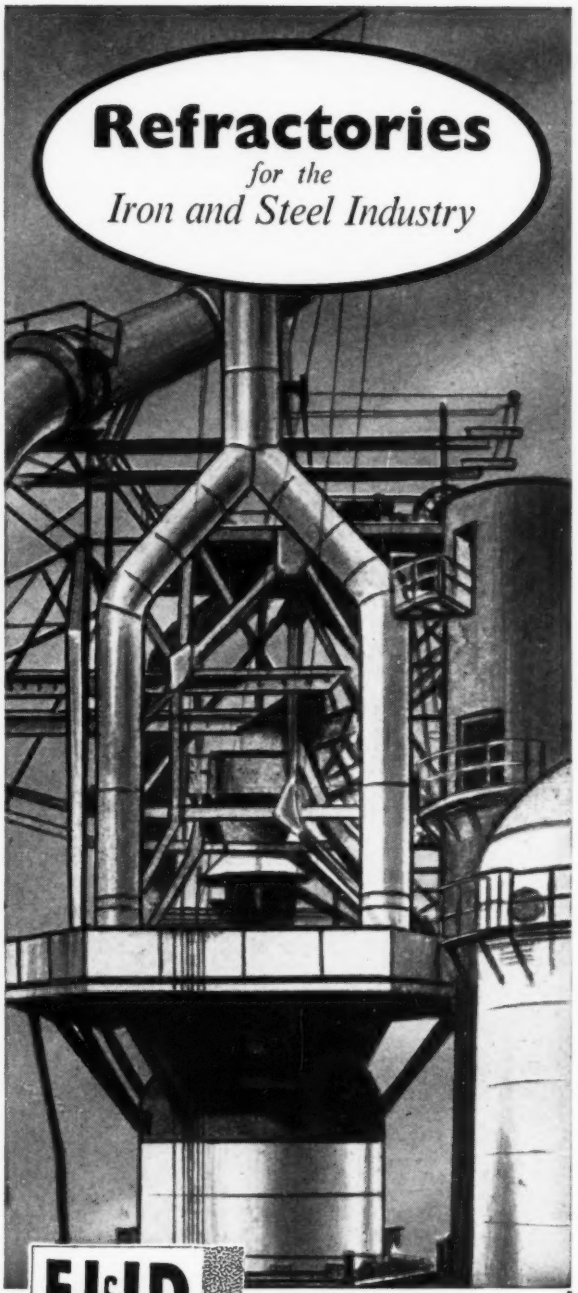

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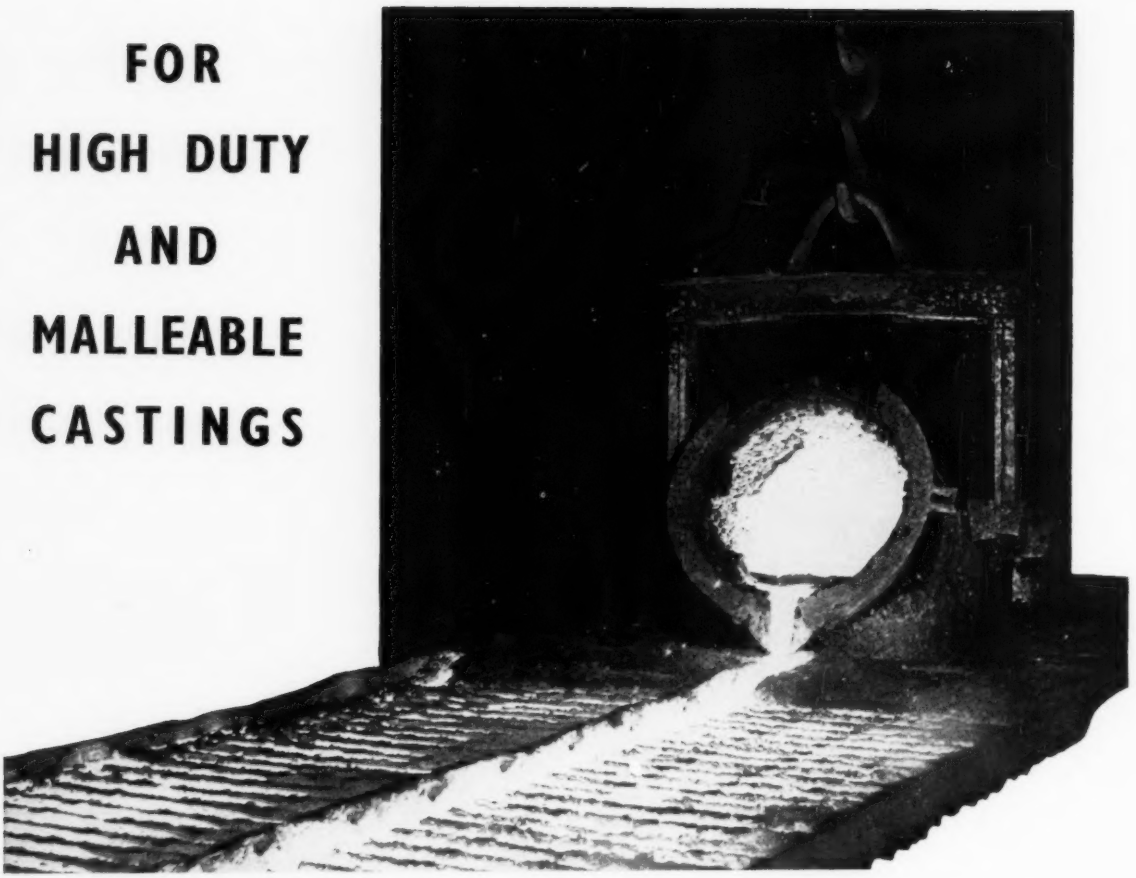

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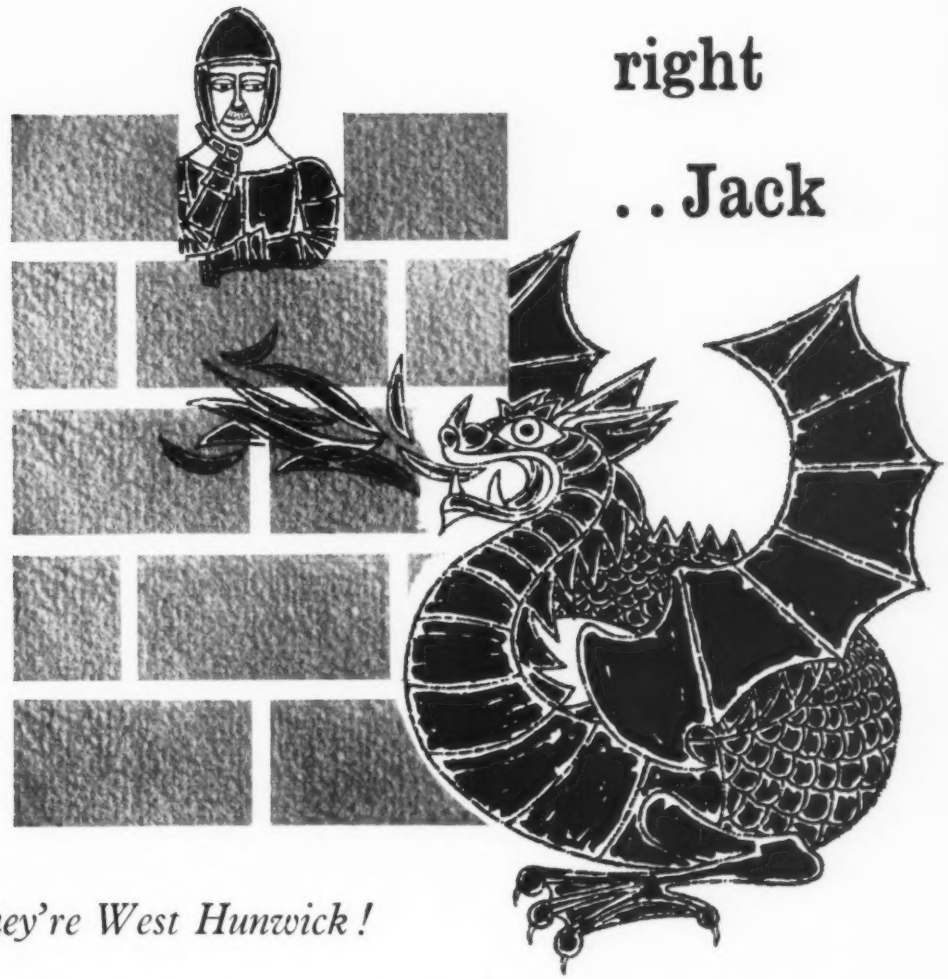

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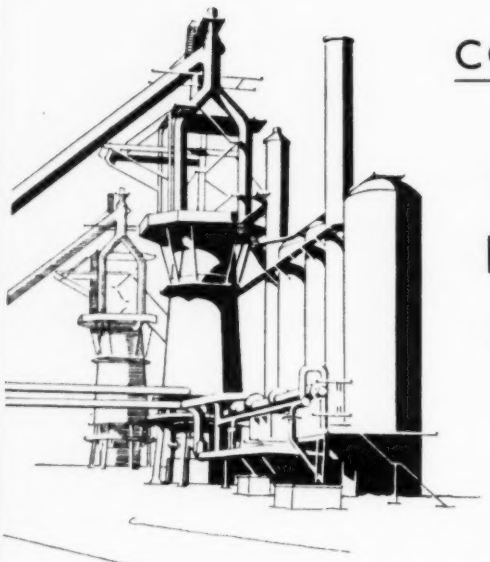

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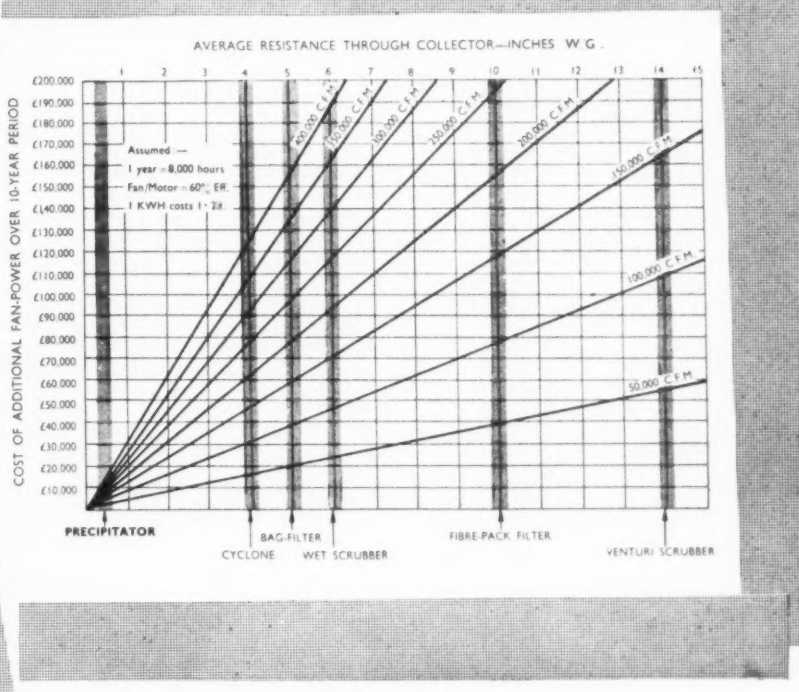

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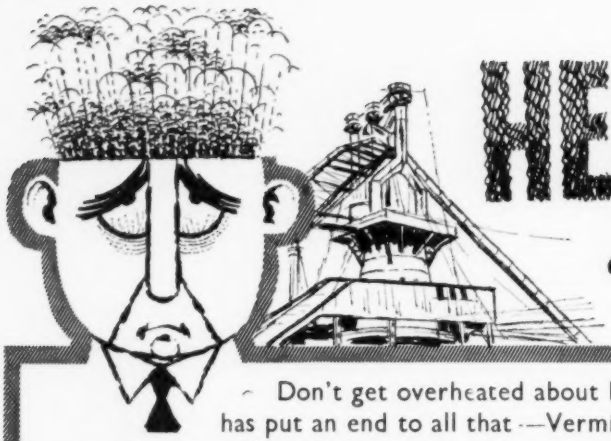

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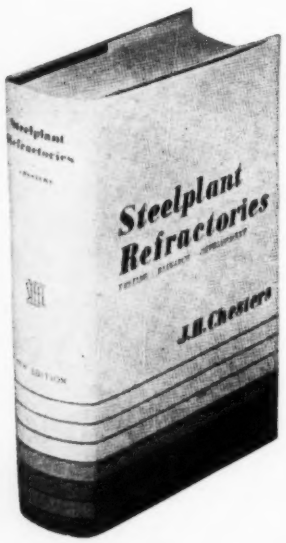

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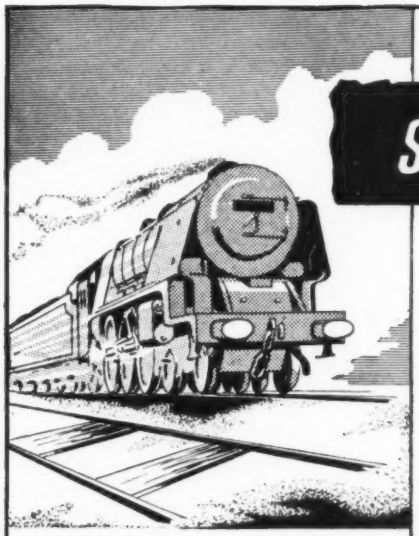

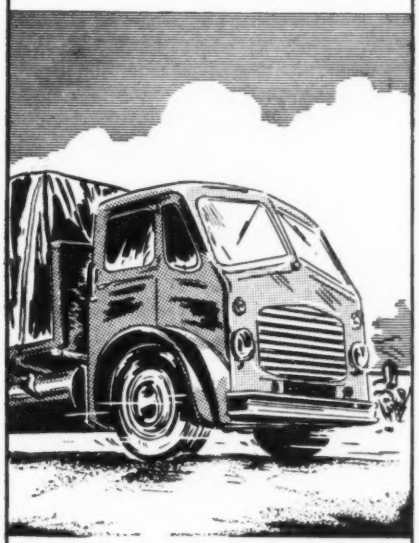

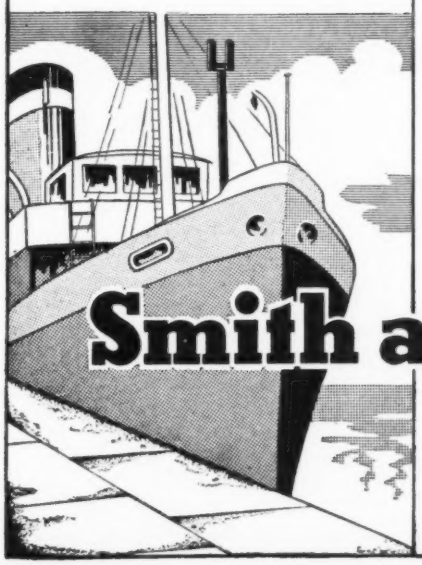

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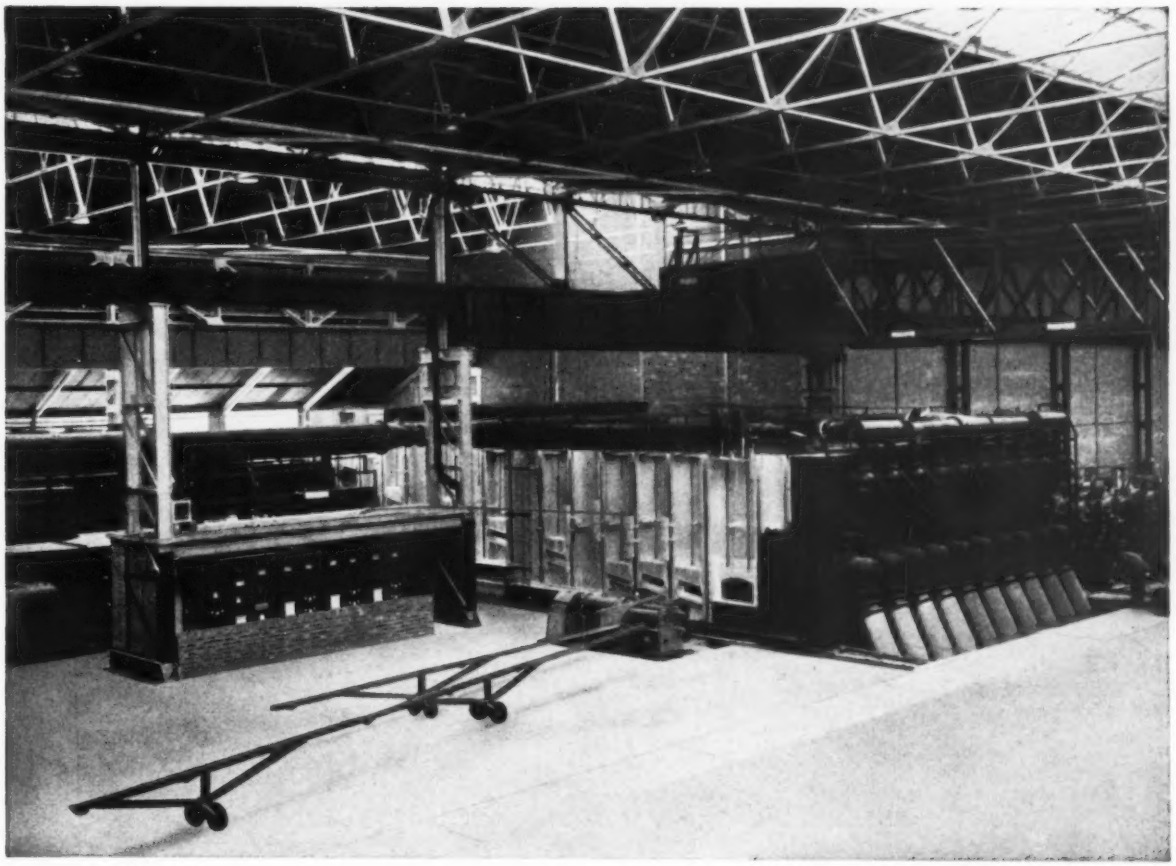

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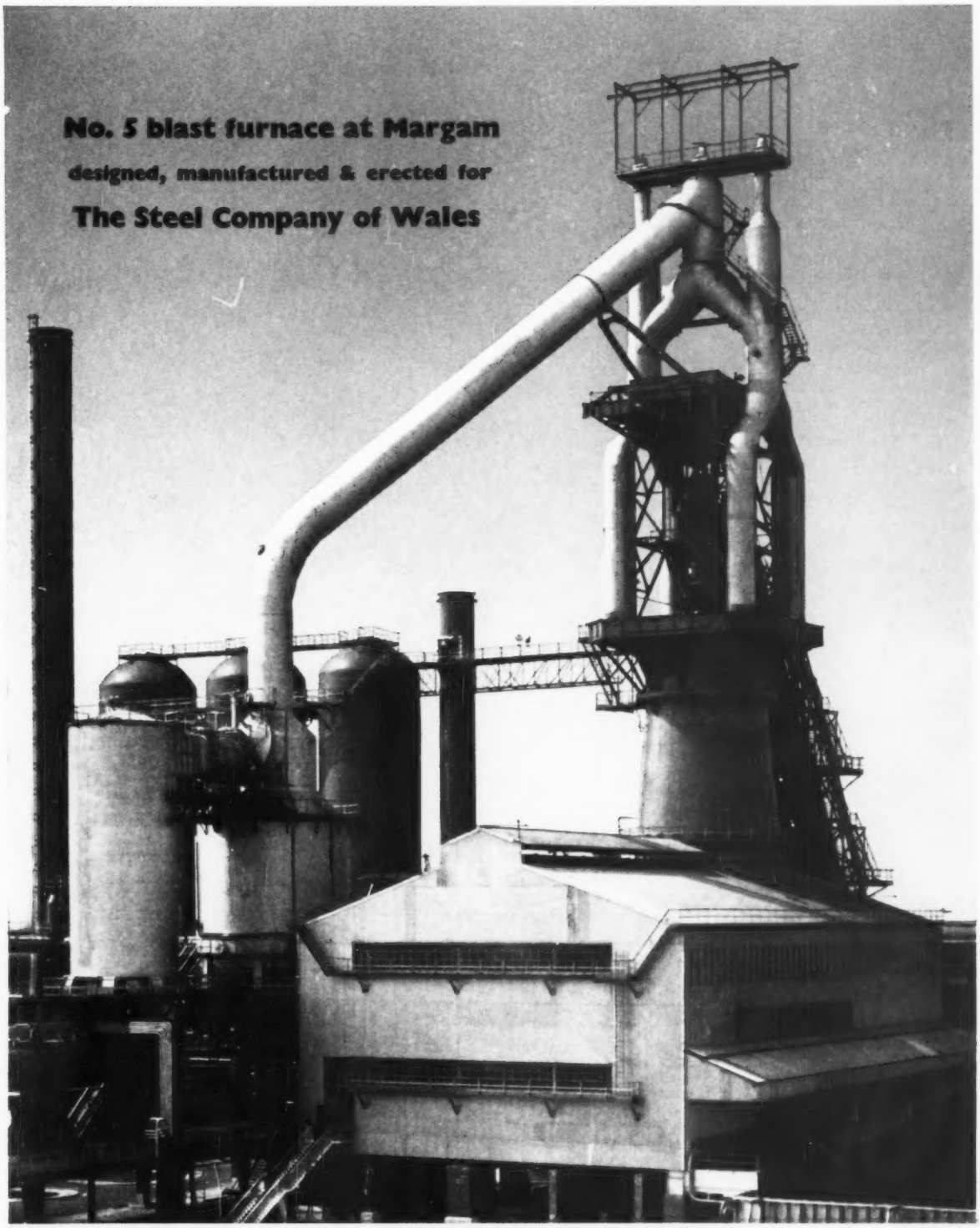

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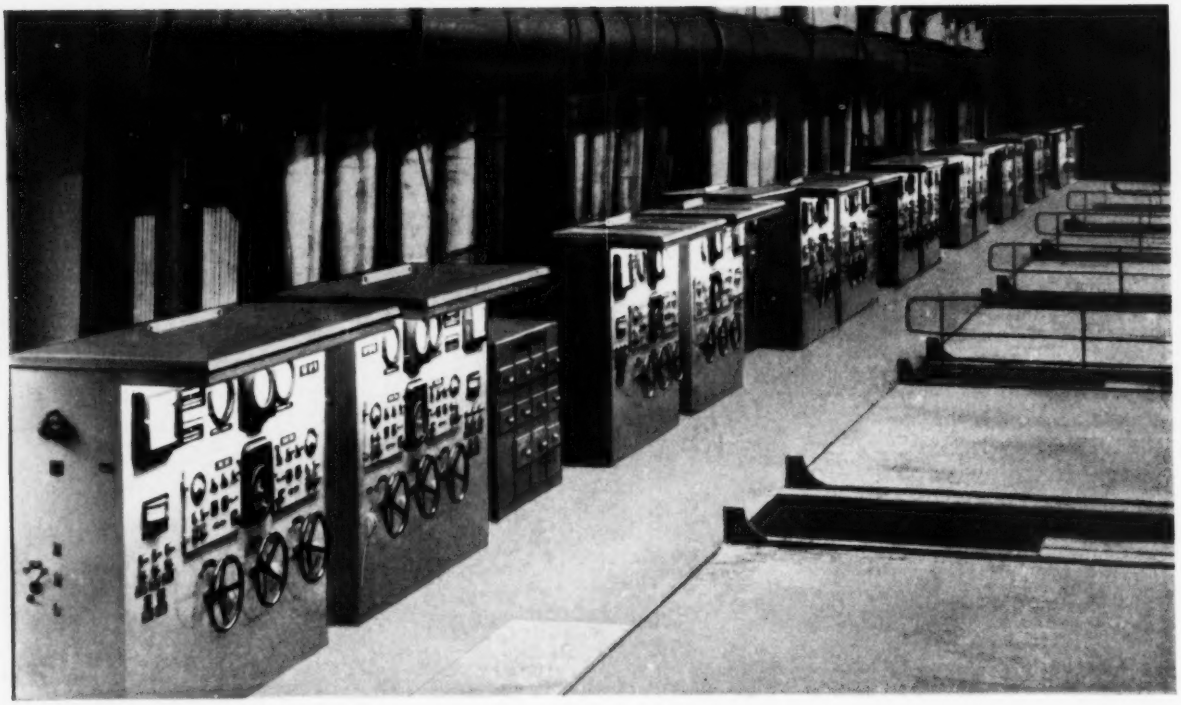

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